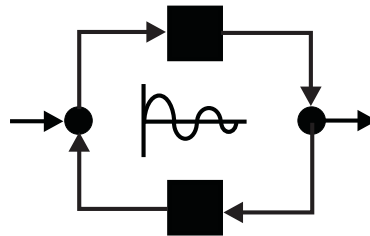


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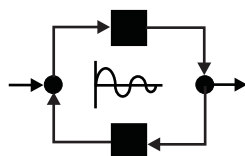
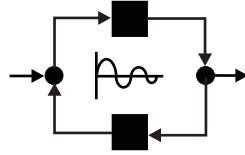


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Available Transfer Capability based Transmission Loading Relief (TLR) in Deregulated Power System

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Abstract

This paper illustrates the relationship between Transmission Loading Relief (TLR) and Available Transfer Capability (ATC), and present possible approaches to reduce the need to invoke TLR procedures while improving the utilization of the transmission system. In open-access electricity markets the reservations of transmission services are based on the data resulting from the off-line computation of ATC. When ATC data is inaccurate or misused, security violations can emerge and cause operators and operation planners to invoke TLR procedures. In this paper ideas are demonstrated on 7-bus, 26-bus and IEEE118 bus system and results have been presented and analyzed¹.

Keywords: Hour-ahead ATC, off-line ATC, Transmission Loading Relief (TLR).

1. Introduction

The concept of competitive market rather than regulated ones has become prominent in the past few years. Economists and political analysts have promoted the idea that free markets can drive down costs and prices thus reducing inefficiencies in power production. This change in the climate of ideas has fostered regulators to initiate reforms to restructure the electricity industry to achieve better service, reliable operation, and competitive rates. Deregulation of the power industry was first initiated in United Kingdom, followed suit in Norway, Australia partly in India.

The North American Electric Reliability Council (NERC) has developed an Operating Manual that reduces a multitude of issues associated with electric power system operations [1]-[3]. This manual includes Transmission Loading Relief (TLR) procedure for dealing with issues that focus primarily on the relationship between interchange transactions and security violations. Since interchange transactions are typically created through the

use of Available Transfer Capability (ATC) information, there should be a very close relationship between the need for TLR and the accuracy and use of ATC information. This paper examines two important aspects of ATC accuracy and use. The first considers the non-simultaneous nature of posted numbers. The second considers the changing of numbers between areas with connecting paths [11].

Quantifying the capabilities of a transmission system for interchange has been of interest for over 25 years. These earlier methods focused primarily on Simultaneous Interchange Capability (SIC) rather than wheeling capabilities. The application of SIC was more of an emergency capability in case local generation was lost and massive import was required. The terminology of Available Transfer Capability (ATC) was created to quantify open access to transmission facilities. As such, it is used to reserve transmission services needed for economic transactions between suppliers and consumers. The theoretical foundations and computational burdens of ATC are essentially identical to those of SIC.

A large percentage of ATC calculations performed today utilize linear load flow techniques to approximately predict changes in line loading in response to transfers and line outages. In their simplest form, these linear methods rely exclusively on distributions factors [16]. The Power Transfer Distribution Factor (PTDF) is a "power" version of traditional linear circuit analysis with current division. The Line Outage Distribution Factor (LODF) also uses traditional linear circuit analysis to compute these "large-change" sensitivities. While some work has been done on including dynamic constraints on transfers, the core ATC computation considers static constraints only. Currently, the following procedure referred to as off-line ATC, is used to provide ATC information for commercial purposes:

1. The security coordinator builds a regional model for the expected peak conditions. The model includes power flow base case, contingencies, monitored elements and directions.

¹ This study has been implemented on Power world simulator at CVR College of Engineering.



2. The security coordinator computes firm and non-firm ATC based on the set of proposed directions on a monthly, weekly and daily basis, and posts the results on the Open Access Same-time Information System (OASIS).
3. Marketers use ATC data to propose transactions and make reservations of transmission services.
4. Control areas verify and debug the OASIS values for the day, including specific system changes and operational considerations.
5. As the time for transaction implementation approaches, control areas perform security analysis to identify potential problems.

The process has been implemented within a daily framework due to important technical reasons related to the fact that ATC is a massive and complicated computational task, and that making its results available demands a large data integration effort. The same reasons have left many ATC issues unsolved and set challenges regarding ATC procedures and computational methods.

2. Background of ATC

The Available Transfer Capability (ATC) is a measure of the transfer capability remaining in the physical transmission network for further commercial activity over and above previously committed uses. Mathematically, ATC is defined as the Total Transfer Capability (TTC) less the Transmission Reliability Margin (TRM) and the Capacity Benefit Margin (CBM) [1].

$$\therefore \text{ATC} = \text{TTC} - \text{TRM} - \text{CBM}$$

TTC is defined as the amount of electric power that can be transferred over the interconnected transmission network in a reliable manner while meeting all of a specific set of defined pre and post-contingency system conditions.

TRM is defined as that amount of transmission transfer capability necessary to ensure that the interconnected transmission network is secure under a reasonable range of uncertainties in system conditions.

CBM is defined as that amount of transmission transfer capability reserved by load serving entities to ensure access to generation from interconnected systems to meet generation reliability requirements.

References [7]-[9] & [12] provide additional analysis of errors associated with ATC.

3. Transmission Loading Relief

Transmission Loading Relief (TLR) is a sequence of actions taken during operations planning or during real-time operation to avoid or remedy security violations associated with the transmission system.

The process starts when a security coordinator identifies a transmission facility within its security area that is about to, or has exceeded the operating security limit. At this point the security coordinator may invoke TLR. This NERC TLR procedure involves following “levels” [1]:

Level 1: Notify reliability coordinators of potential operating security limit Violations.

Level 2: Hold transfers at present level to prevent operating security limit Violations.

Level 3a: Reallocation of Transmission Service by curtailing interchange transactions using non-firm point-point transmission service to allow interchange transactions using higher priority transmission service.

Level 3b: Curtail interchange transactions using Non-Firm Transmission Service Arrangements to mitigate an operating security limit Violation

TLR Level 4: Reconfigure Transmission

TLR Level 5a: Reallocation of Transmission Service by curtailing interchange transactions using Firm Point-to-Point Transmission Service.

TLR Level 5b: Curtail interchange transactions using Firm Point-to-Point Transmission Service to mitigate an operating security limits Violation.

TLR Level 6: Emergency Procedures.

TLR Level 0: TLR concluded.

4. Types of Transactions

In open-access electricity markets transactions can be done in three different ways. They are:

- A. Simultaneous transactions
- B. Sequential transactions: off-line ATC
- C. Sequential transactions-updated ATC

A. Simultaneous transactions

In a system, suppose that the transmission reservations occur simultaneously [4], [5]. If these transactions are implemented, there is a chance that one or more lines may be overloaded. When this schedule is analyzed prior to implementation, TLR will have to be invoked to relieve the overloading of these lines. Each participating control area for the transaction will implement a local TLR procedure, which would imply the rearrangement of its own resources, or ask the security coordinator for a TLR procedure, which would require the rearrangement of the resources of all the interconnection entities involved in the transaction.

Once TLR is initiated, the process determines for each type of transaction the impact of those that affect the loading of the facility in 5% or more based on PTDFs [6]. Then transactions are curtailed in a progressive, prioritized manner. If no more transactions can be curtailed, the process goes to the next TLR level.

B. Sequential transactions: off-line ATC

As an alternative approach, consider a sequence of transactions starting from base case. In this case, ATC values remain as those computed by the off-line ATC process as the transactions take place.

- 1) At time t_1 marketer M1 proposes a transaction in one direction. The transfer should create no problem if there is ATC available after the transaction is implemented.
- 2) At time t_2 marketer M2 reads ATC for another direction and proposes a transaction T2. There may be a chance of overload if the transaction T2 is made considering the ATC values prior to transaction T1 that needs to be relieved. So, TLR procedure should be invoked to relieve this overload.

Note in this case that for proposed transactions the values of unreserved ATC do not change and therefore the capability for each direction is assumed to be the one that



was computed for the base case. As this is a non-simultaneous process, new transactions can overload the system. TLR will be needed even though the transactions were based on posted ATC values.

C. Sequential transactions-updated ATC

Because of the disadvantage of invoking TLR procedures in the Sequential Off-Line ATC method, we go for this case. Start again with the system at the initial operating point.

- 1) At time t1 marketer M1 proposes a transaction in one direction. The transfer should create no problem if there is ATC available after the transaction is implemented.
- 2) ATC values are recomputed and updated.
- 3) At time t2 marketer M2 reads ATC for another direction and proposes a transaction T2. As the transaction is made based on the updated ATC values, there should be no overload.
- 4) ATC values are recomputed and updated after each transaction.

This case is an ideal case in which ATC is updated each time a transaction takes place, is proposed, or a reservation is made. Note that ATC values remain positive. For this ideal case, no TLR procedure is required [17].

If a continuous ATC computation is possible, and transactions are based on updated ATC data we can think of transactions as moving within a secure transactional space.

5. Case Study

A. 7-Bus System

The computations were tested in 7-bus system with 3-areas. The system has been divided into three areas, namely; Area A, Area B and Area C. Area A includes buses 1, 2, 3, 4 and 5. Area B consists of bus 6 whereas Area C consists of bus 7. ATC values at initial operating are shown in Table 1 [13].

Table 1. 7-Bus case, ATC Data

Transfer Areas	Transfer Buses	ATC (MW)	Limiting Line
A-B	1-6	54	1-2
	2-6	265	2-6
	4-6	281	2-6
A-C	1-7	56	1-2
	2-7	106	2-5
	4-7	152	4-5
B-C	6-7	111	6-7

Given this system data, suppose that the following transmission reservations occur, which are all possible after the initial posting:

- Direction 1-6:50MW
- Direction 2-6:200MW
- Direction 4-6:200MW

If these transactions are implemented line 1-2 will have over load of 22% and line 2-6 will have an over load of 95%. When this schedule is analyzed prior to implementation, TLR will have to be invoked to relieve the overloading of these lines. Sequential ATC data for this example is given in Table 2. If all the ATC numbers were computed after t3 the results would be as shown in

Table 3. Sequential transactions-update ATC data for this case is given in Table 4.

Table 2. Sequential ATC Data

Transfer		t0		t1		t2		t3	
Area	Bus	ATC MW	Limit Line	ATC MW	Limit Line	ATC MW	Limit Line	ATC MW	Limit Line
A-B	1-6	54	1-2	4	1-2	4	1-2	4	1-2
	2-6	265	2-6	265	2-6	65	2-6	65	2-6
	4-6	281	2-6	281	2-6	281	2-6	81	2-6
A-C	1-7	56	1-2	56	1-2	56	1-2	56	1-2
	2-7	106	2-5	106	2-5	106	2-5	106	2-5
	4-7	152	4-5	152	4-5	152	4-5	152	4-5
B-C	6-7	111	6-7	111	6-7	111	6-7	111	6-7

Table 3. ATC for Final Conditions

Transfer		t3	
Area	Bus	ATC MW	Limit Line
A-B	1-6	-168	1-2
	2-6	-166	2-6
	4-6	-186	2-6
A-C	1-7	-448	1-2
	2-7	-438	2-5
	4-7	-513	4-5
B-C	6-7	87	6-7

If a continuous ATC computation is possible, and transactions are based on updated ATC data we can think of transactions as moving within a *secure transactional space* as shown in Figure. 1.

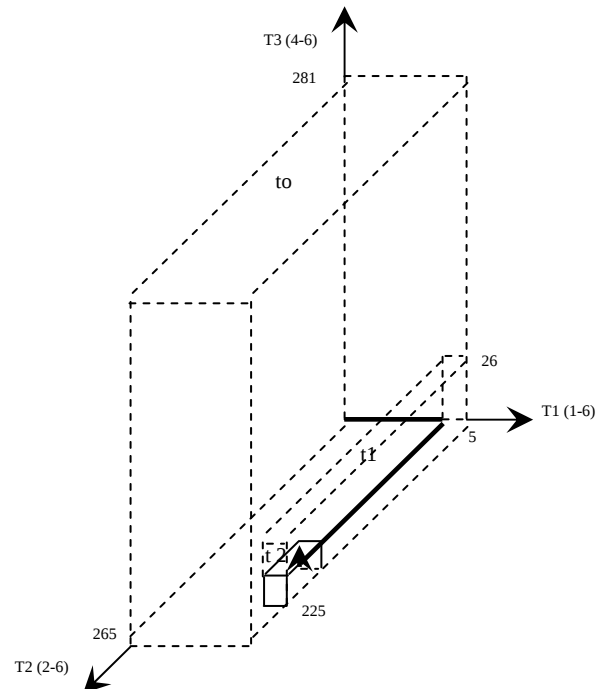


Figure 1. Secure Transactional Space for transactions of 7-bus System

Secure transactional space A secure transactional space (as mentioned earlier) is now created based on the above Sequential Updated ATC data. This space gives the limits for future transactions and all transactions are to be made within its limits to reduce the chance of overloading.

It can be seen that each time a transaction is proposed, the secure transactional space changes (it can be reduced or augmented).



Table 4. Sequential Transactions-Update ATC

Transfer		t0		t1		t2		t3		t4	
Area	Bus	ATC	Limit	ATC	Limit	ATC	Limit	ATC	Limit	ATC	Limit
		MW	Line	MW	Line	MW	Line	MW	Line	MW	Line
A-B	1-6	54	1-2	4	1-2	9	1-2	4	1-2	54	1-2
	2-6	265	2-6	220	2-6	25	2-6	13	2-6	59	2-6
	4-6	281	2-6	28	1-2	26	2-6	14	2-6	62	2-6
A-C	1-7	56	1-2	6	1-2	9	1-2	5	1-2	56	1-2
	2-7	106	2-5	98	2-5	54	2-5	34	2-6	63	2-5
	4-7	152	4-5	34	1-2	57	1-2	30	1-2	90	2-5
B-C	6-7	111	6-7	123	6-7	68	2-5	69	2-5	79	2-5

For the transactional space shown in the above Fig. 1, the coordinates are the transactions in directions 1-6, 2-6 and 4-6. The center of coordinates (0, 0, 0) represents the initial operating point where no transactions take place. At this point the secure transactional space is given by cube t0 with dimensions {54, 265, 281}. Then T1 results in a secure transactional space given by cube t1 with dimensions {4, 220, 28}. T2 moves the operating point to (50, 200, 0) and ATC results in a secure transactional space given by cube t2 with dimensions {9, 25, 26}. T3 moves the operating point to (50, 200, 10) and so on. As it can be seen, each time a transaction is proposed, the secure transactional space changes (it can be reduced or augmented) after ATC values and the distances to each boundary are given by the ATC value for that direction.

B. 26-bus System

A 26-bus power system, taken from an example in a book by Sadat [10] is considered as a single area. Suppose that the following transmission reservations occur, which are all possible after the initial posting:

- Direction 1-18:200MW
- Direction 2-12:150MW
- Direction 4-9:150MW

If these transactions are implemented, line 1-18 will have over load of 7% and line 7-9 and 4-12 will have an over load of 8%. When this schedule is analyzed prior to implementation, TLR will have to be invoked to relieve the overloading of these lines. Sequential transactions-update ATC data for this case is given in Table 5. *Secure transactional space* for this case is shown in Figure. 2.

C. IEEE 118- Bus System

For the case study, a large sized system, IEEE 118-bus system is composed of 54 generators with a total installed capacity of 8190MW and the system demand is 3668MW. The system is divided into four areas as follows. For this analysis each area is considered as one generating company. The details of areas are given in Tables 6.

Table 5. Details of Areas of IEEE 118-bus System

Area	Buses
A	1 – 38, 113 – 115 & 117
B	39 - 68
C	69 – 102, 116 & 118
D	103 - 112

Suppose that the following transmission reservations occur, which are all possible after the initial posting:

- Direction 25-59:700MW
- Direction 100-45:700MW
- Direction 80-11:700MW

If these transactions are implemented, line 8-5 will have over load of 24% and line 68-81 and 80-81 will have an over load of 31%. When this schedule is analyzed prior to implementation, TLR will have to be invoked to relieve the overloading of these lines. Sequential transactions-update ATC data for this case is given in Table 6. *Secure transactional space* for this case is shown in Figure. 3.

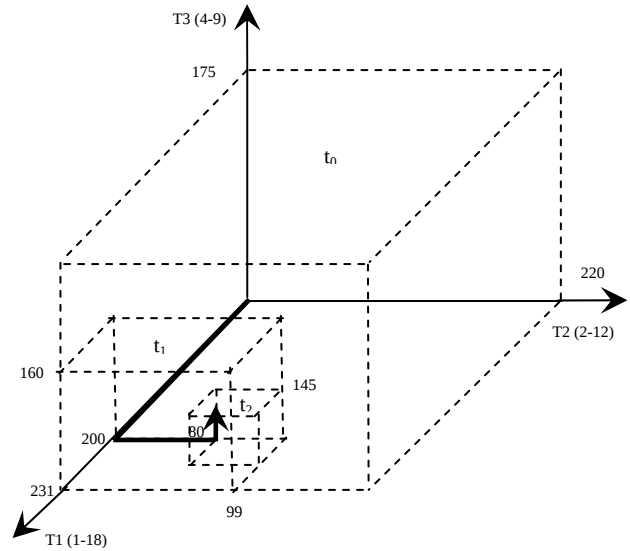


Figure 2. Secure Transactional Space for transactions of 26-bus System.

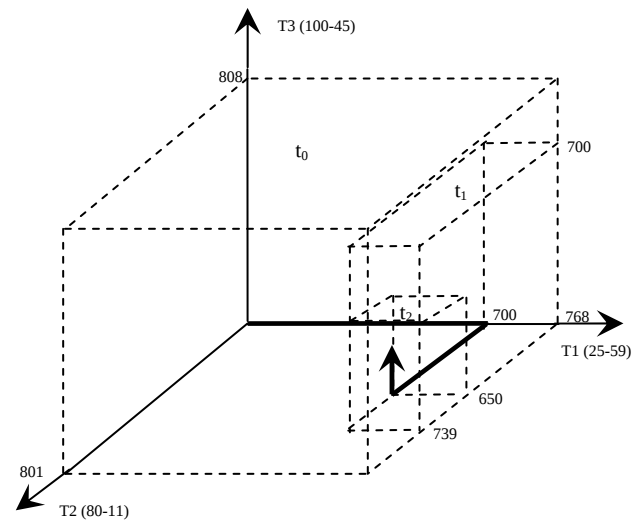


Figure 3. Secure Transactional Space for transactions of 118-bus System



Table 6. Sequential Transactions-Update ATC for 26-bus System

Transfer	t0		t1		t2		t3		t4	
Bus	ATC MW	Limit Line	ATC MW	Limit Line	ATC MW	Limit Line	ATC MW	Limit Line	ATC MW	Limit Line
1-18	231	1-18	31	1-18	5	1-18	1	1-18	205	1-18
1-12	240	2-8	74	1-18	15	1-18	3	1-18	165	2-8
2-12	220	2-8	99	1-18	19	1-18	4	1-18	151	2-8
3-9	113	3-13	93	3-13	40	1-18	7	1-18	86	3-13
4-9	175	12-10	162	10-12	146	1-18	46	1-18	95	7-9
4-18	190	16-17	42	1-18	9	1-18	2	1-18	112	4-12
26-18	130	5-6	47	1-18	10	1-18	2	1-18	83	11-26

Table 7. Sequential Transactions-Update ATC for IEEE 118-bus System

Transfer		t0		t1		t2		t3	
Area	Bus	ATC	Limit Line	ATC	Limit Line	ATC	Limit Line	ATC	Limit Line
		MW		MW		MW		MW	
A-B	25-59	765	64-65	068	64-65	065	64-65	060	64-65
	26-62	841	64-65	352	64-65	335	64-65	246	64-65
A-C	12-77	794	65-68	862	65-68	965	65-68	1048	65-68
	26-90	836	65-68	909	65-68	1000	89-90	1007	89-90
B-A	54-11	861	38-65	835	38-65	770	38-65	665	38-65
	65-27	842	38-65	816	38-65	754	38-65	650	38-65
B-C	54-77	697	65-68	758	65-68	848	54-56	864	68-69
	65-82	580	65-68	630	65-68	705	65-68	673	68-69
C-A	80-11	801	65-68	739	65-68	90	81-80	89	81-80
	92-32	910	65-68	835	65-68	358	81-80	100	81-80
C-B	87-54	300	86-87	300	86-87	300	86-87	93	81-80
	100-45	808	86-87	700	65-68	327	81-80	77	81-80

6. Chaining ATC Numbers

When a transaction involves several control areas and associated ATC postings, the need to reserve transmission services along a contract path can result in a misuse of ATC numbers. For example, if transmission services are required between areas A, B and C for an A to C transaction, reservations might be made based on the minimum capability posted for A to B and B to C. This is a logical assumption, but can result in a TLR procedure because this chaining is not normally valid. One of the primary reasons why it is not valid is because ATC numbers are based on maximum allowable transfers subject to numerous contingencies. It is very likely that the limiting transfer will occur because of a proposed contingency. Since the A to B transfer might be limited by one contingency and the B to C transfer might be limited by a different contingency, it is not correct to use the minimum of these two ATC numbers to determine the A to C maximum transfer. It may well be that the A to C transfer is actually limited by a completely different contingency than the A to B and B to C. Furthermore, the ATC numbers may be the result of different limiting elements. There is one case where the ATC number for A to C can be obtained from the ATC numbers for A to B and B to C, but the result is not simply the minimum of the two. Consider the following new line ij flows in response to a transaction of P from A to B, B to C and A to C:

$$P_{ij, AB}^{new} = P_{ij}^{old} + T_{ij, AB}^{new} P$$

$$P_{ij, BC}^{new} = P_{ij}^{old} + T_{ij, BC}^{new} P$$

$$P_{ij, AC}^{new} = P_{ij}^{old} + T_{ij, AC}^{new} P$$

For a maximum allowable line ij flow of P_{ij}^{max} , the maximum P which can be used for each transaction is:

$$P_{max AB} = \frac{(P_{ij}^{max} - P_{ij}^{old})}{T_{ij, AB}}$$

$$P_{max BC} = \frac{(P_{ij}^{max} - P_{ij}^{old})}{T_{ij, BC}}$$

$$P_{max AC} = \frac{(P_{ij}^{max} - P_{ij}^{old})}{T_{ij, AC}}$$

Where all of the P 's and T 's are assumed to be positive numbers. In this case, P_{maxAC} is the correct ATC for a transaction from A to C. If only the ATC postings for A to B and B to C are given, the ATC for A to C can be computed from the other two as (by inspection from above, since

$$T_{ij, AC} = T_{ij, AB} + T_{ij, BC}$$

$$P_{max AB} = \frac{(P_{max AB} P_{max BC})}{(P_{max AB} + P_{max BC})}$$

Note that this is not normally the same as the minimum of P_{maxAB} and P_{maxBC} . Also note that this computation may need to be revised when one or more of the T 's are negative numbers, or a line flow maximum is reached through a negative flow. Also note that this is for the special case where all ATC numbers are determined by the same limiting line and the same contingency.

Further investigation into the use of posted ATC for chaining needs to be done. It may be that numbers other than ATC should be used. For example, perhaps PTDF's should be posted rather than the ATC. Then ATC can be computed when needed for any transaction [15].

As a compromise between offline ATC computation and real-time ATC, the implementation of hour-ahead ATC would be consistent with hourly market decisions. This would solve a large number of the problems. The following section gives a framework for this.



7. Hour-Ahead ATC

Hour-ahead ATC performs the same computation as off-line ATC. It operates though over the planned model for the next few hours. It would be used intensively within the Energy Management System (EMS) study mode, imposing hard performance and data integration challenges.

Current ATC solvers are able to provide accurate results for systems with thousands of buses, and hundreds of directions and contingencies in few minutes. The difficult part of hour-ahead ATC is data management and modeling.

To accurately compute ATC, the following information must be provided to the solver:

System and Transaction data: network topology, equipment parameters, control settings, load profile, generation profile, external model description, interchange schedule, contingency description, subsystem description, direction description.

From existing technologies, ATC for the next hour can be computed as follows.

- 1) Obtain a base system case from the real-time state estimator solution. This would give a base topology description, generation schedule and base power flows.
- 2) Include in the system model modifications to loads using system and bus load forecast functions. Update generation settings using generation schedules and update base flows using information for schedules and transmission services.
- 3) Solve the power flow for the next hour case and compute PTDFs and LODFs matrices.
- 4) Compute ATC for confirmed reservations using a transaction analyzer based on linear methods which could also be used for other computations such as contingency analysis, sensitivity analysis, interchange distribution calculations, transaction arrangement and transmission loading relief [14].

Hour-ahead ATC would run cyclically (every hour) over future hour models. It could also run on operator request and when new reservations have been confirmed [17]. The function would be the core of the information system, serving both the economic and reliability goals of the hourly-market. A flow chart of the functions is shown in Figure 4.

8. Conclusion

TLR procedures are implemented to relieve overloading in the transmission system. TLR is required because proposed transactions are based on ATC values that do not necessarily reflect the actual transfer capabilities either due to the non-simultaneous nature of ATC, or improper chaining. In order achieve an efficient and stable performance of the hourly market; the participants need to have accurate ATC information. A way to increase the quality and meaningfulness of ATC data is to compute it in such a time framework that the model included in the computation is the closest to the real situation. In the ideal case, marketers would have updated information each time a reservation is confirmed.

With existing technologies it may be possible to incorporate hour-ahead ATC functions that minimize the need for TLR as well as enhance the utilization of transmission resources. The reduction of the need for TLR may also be achieved by minimizing misuse of ATC numbers, or through alternative concepts that are more accurate and flexible for use in reserving contract paths.

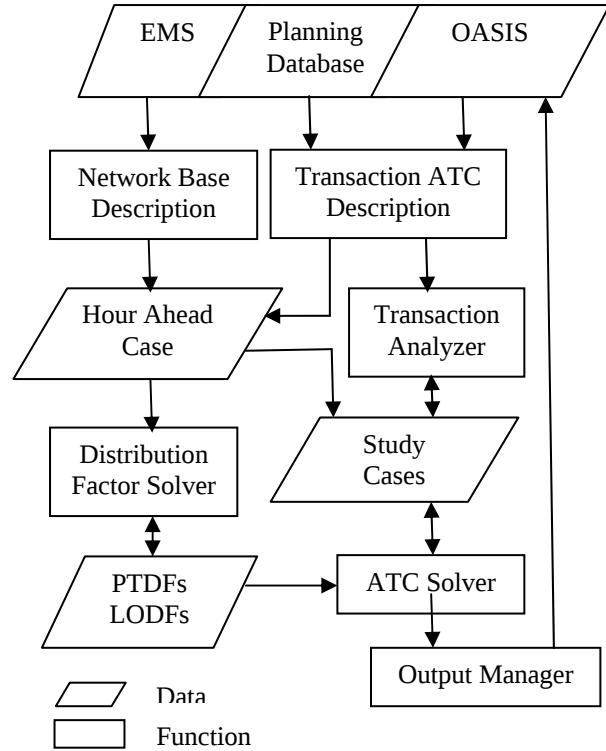


Figure 4. Hour-Ahead ATC

9. Acknowledgements

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Nomenclature

$T_{ij,AB}$	Transaction of Real power for line ij from area A to area B
$T_{ij,BC}$	Transaction of Real power for line ij from area B to area C
$T_{ij,AC}$	Transaction of Real power for line ij from area A to area C
P_{ij}^{old}	Real power in line ij before the transaction T_{ij}
$P_{ij,AB}^{new}$	Real power in line ij from area A to B after the transaction $T_{ij,AB}$
$P_{ij,BC}^{new}$	Real power in line ij from area B to C after the transaction $T_{ij,BC}$
$P_{ij,AC}^{new}$	Real power in line ij from area A to C after the transaction $T_{ij,AC}$
P_{ij}^{max}	Maximum allowable power flow in line ij
P_{maxAB}	Maximum power that can be transferred from area A to area B
P_{maxBC}	Maximum power that can be transferred from area B to area C
P_{maxAC}	Maximum power that can be transferred from area A to area C



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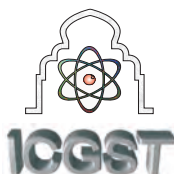




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Voltage Instability and Voltage Collapse as Influenced by Cold Inrush Current

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Abstract

The occurrence of voltage instabilities or voltage collapses depend on the duration of the persistence of the fault and on the type of fault, some faults lead to voltage instabilities, others lead to voltage collapse. Evaluation of fault durations causing occurrence of voltage instabilities or collapse is the main goal of this paper. This paper searches for the effect of peak inrush current and its duration periods initiation of voltage instability, with lagging and leading load power factors at certain loads buses. Also, this paper searches about the fault duration required for voltage collapse. The influence of the cold inrush current on this fault duration is studied. Form several tedious trials, it is noticed that, the phenomena of voltage collapse can be occurred with some line opening in the studied system. In this paper, the power system dynamic simulation program is developed for dynamic analysis of voltage stability.

Keywords: *Power System Stability - Voltage Instability – Voltage Collapse – Cold Inrush Current.*

1. Introduction

Voltage stability is currently one of the most important research areas in the field of electrical power systems. With the increased loading and exploitation of the power transmission system being also due to improved optimized operation the problem of voltage stability and voltage collapse attracts more and more attention [1-2]. Voltage collapse can take place in systems or subsystems and can appear quite abruptly which requires the improved continuous monitoring of the system state [1]. The problem of voltage collapse may be simply explained by an inability of the power system to supply the reactive power or by an excessive absorption of reactive power by the system itself. It is to be understood as a reactive problem and it is strongly affected by the load behavior [2-3]. The change in voltage is so rapid that voltage controls devices may not take corrective actions rapidly enough to prevent cascading blackouts [3-5]. The continual increase in demand for electric power has forced utility companies to operate their systems closer to the limits of instability [6-7].

Voltage instability concerns voltage fluctuations around nominal values. These fluctuations are either periodic or non periodic [8-9]. The main factor causing voltage instability is the inability of the power system to meet the demand for service power. The heart of the problem is usually the voltage drop that occurs when active and reactive power flow through the series inductive reactances of the transmission network [10-13]. A criterion for voltage stability is that, at a given operating condition for every bus in the system, the bus voltage magnitude increases as the reactive power injection at the same bus is increased. A system is unstable if for at least one bus in the system, the bus voltage magnitude decreases as the reactive power injection at the same bus is increased. In other words, a system is voltage unstable if V-Q sensitivity is negative for at least one bus [14-15]. Progressive drop in bus voltage can also be associated with rotor angles going out of step. For example, the gradual loss of synchronism of machines as rotor angle between two groups of machines approach or exceed 180 degree would result in very low voltages at intermediate points in the network. Voltage instability is essentially a local phenomenon; however, its consequences may have widespread impact [14]. Voltage collapse is a rapid progressive voltage fall and settling at certain value defined by system parameters, it is more complex than simple voltage instability and usually the result of a sequence of events accompany voltage instability leading to a low voltage profile in a significant part of the power system lasting long periods [16-17]. Features and modelling capabilities of conventional transient stability time domain simulation programs has been greatly enhanced over recent years to make them suitable for the assessment of voltage stability problems.

In this paper, the power system dynamic simulation program is developed for dynamic analysis of voltage stability. The developed program has the modelling capability to account for dynamics, which are important in voltage stability analysis including those associated with dynamic loads and generators and their associated control. In this paper, studies the effect of the peak inrush currents as a disturbance on the duration period which causes voltage instability, with different lagging and



leading loads power factors. When inrush current is considered as a disturbance by increasing its peak value for many times of the normal operating system current, the duration period of this inrush current at which voltages will be unstable is determined for various values of initial peak inrush current from that network. The effect of the peak inrush currents on the fault duration causing to reach voltage collapse, when after certain steady state is reached, the line between two buses is opened, with different lagging and leading loads power factors.

2. Power System Model Representation

The power system considered consisting of n synchronous generators feeding through a transmission network, a number of loads. The system motion using one-axis generator model, under a disturbance, the following set of differential equations described for each generator [18]:

$$\dot{\delta}_i = 2\pi f_{0i} \omega_i \quad (1)$$

$$\dot{\omega}_i = (P_{mi} - P_{ei} - D_i \omega_i) / 2H_i \quad (2)$$

$$\dot{E'_{qi}} = (E_{fdi} - E'_{qi} - (X_{di} - X'_{di})I_{di}) / T'_{doi} \quad (3)$$

Where: δ , f_0 , ω , H , D , and E'_{qi} are the rotor angle, initial frequency, speed deviation, inertia constant, mechanical damping coefficient, and the q-axis voltage component, respectively. P_m , P_e , E_{fd} , I_d , and T'_{d0} are the generator input mechanical power, output electrical power, excitation voltage, d-axis current component, and d-axis transient open-circuit time constant, respectively. Finally, X_d and X'_d are the d-axis and transient d-axis reactance's.

Note that, P_e is computed as:

$$P_{ei} = E'_{qi} I_{qi} + E'_{di} I_{di} \quad (4)$$

Where, E'_d is the d-axis component of the voltage E' , and I_q is the q-axis component of the generator current, which is given as:

$$I_i = (E'_i - V_i) / (jX'_{di}) \quad (5)$$

Where, E' is the voltage behind the reactance X'_d , and V is the generator terminal voltage.

In this paper, we concerned with the study of the quasi static loads which it consists of heating and lighting equipments. Also, three quasi static models are used for induction motor load representation. The commonly used representations of static loads are either constant impedance to ground, constant current and constant real and reactive power. These load models are given in the following, where P_L , Q_L and V_L are the load active power, reactive power, and bus voltage, respectively (their initial values are P_{Lo} , Q_{Lo} and V_{Lo}).

Constant Impedance Model (C.Z.):

$$P = P_0 \left(\frac{V}{V_0} \right)^2, \quad Q = Q_0 \left(\frac{V}{V_0} \right)^2 \quad (6)$$

Constant Current Model (C.I.):

$$P = P_0 \left(\frac{V}{V_0} \right), \quad Q = Q_0 \left(\frac{V}{V_0} \right) \quad (7)$$

Constant Power Model (C.P.):

$$P = P_0 \left(\frac{V}{V_0} \right)^0, \quad Q = Q_0 \left(\frac{V}{V_0} \right)^0 \quad (8)$$

Three models are used for induction motor load, and the active and reactive power has expressed by the 4th order polynomials representation [14]: Induction motors with constant mechanical loads torque ($T=Const.$):

$$P = 0.275 + 8.79V - 23.27V^2 + 23.233V^3 - 8.017V^4$$

$$Q = -16.11 + 92.94V - 179.72V^2 + 148.56V^3 - 44.67V^4 \quad (9)$$

$$P = -33 + 161.67V - 286.033V^2 + 233.033V^3 - 64.67V^4$$

$$Q = 48.83 - 175.67V + 237.5V^2 - 142.22V^3 + 32.56V^4 \quad (10)$$

$$P = 9.233 - 81.567V + 309.9V^2 - 598.5V^3 + 627.967V^4 - 342V^5 + 75.967V^6$$

$$Q = 46.739 - 53.15V + 1721.67V^2 - 325V^3 + 3488.704V^4 - 1900V^5 + 422.037V^6 \quad (11)$$

The interrelation of the system elements is shown in Figure (1). The three systems solution is the dynamic simulation program, the network reduction program and the load flow program with loads represented by polynomial models. They should be used in sequence to solve the system equations. A flow chart of multi-machine power systems [49], written in MATLAB TOOLBOX is shown in Figure (2).

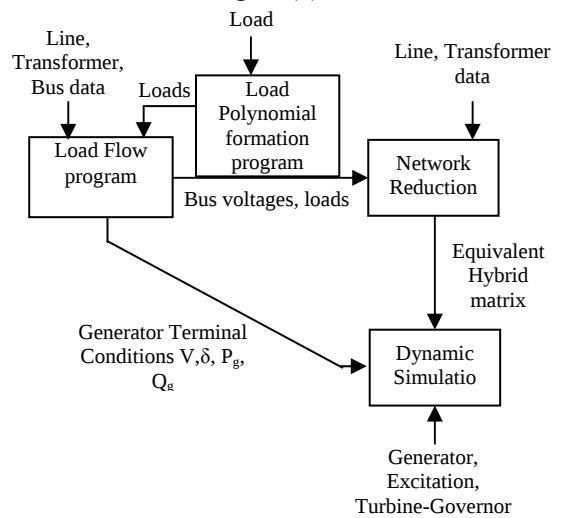


Figure 1. Data requirements of power system simulation program and its support programs



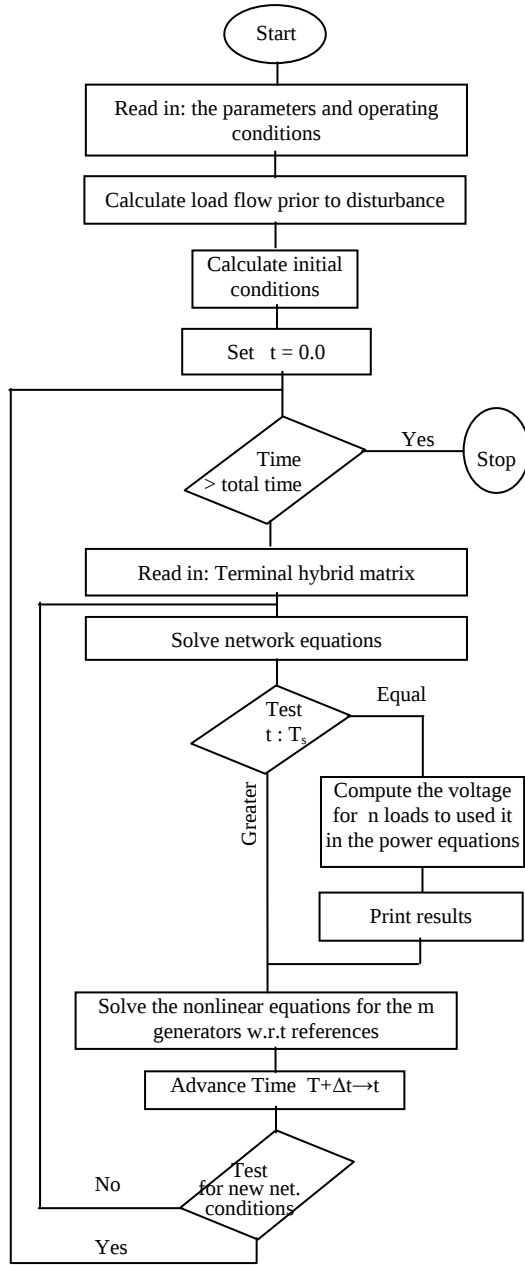


Figure 2. Digital simulation of multi-machine systems, flow chart

The dynamic simulation program reads the nodal initial conditions, equivalent matrix, the generator, excitation, turbine-governor and finally the type of loads data. The output of the dynamic simulation program is the system open loop performance. The system performance is determined by solving the machine mathematical models together with the constraints imposed by the network. The non-linear machine models are solved numerically using the appropriate integration technique. The system performance is determined by solving the machine mathematical models together with the constraints imposed by the network. The non-linear machine models are solved numerically using the appropriate integration technique.

For network having total N buses the calculated voltage at any bus is described by the following nonlinear equations:

$$E_K = \frac{1}{Y_{KK}} \left(\frac{P_K - jQ_K}{E_K^*} - \sum_{j=1, j \neq K}^N Y_{Kj} E_j \right) \quad (12)$$

With: $K=1, 2, \dots, N$, and $K \neq S$, where S means slack bus, E_K is voltage at node K , E_K^* is E_K conjugate, Y_{KK} Self admittance at node K , Y_{Kj} The mutual admittance between node K and node j , E_j Voltage at node j , P_K, Q_K Scheduled real and reactive power entering the system at bus K .

$P_K = P_{ko}(a_0 + a_1 S + a_2 S^2 + a_3 S^3 + a_4 S^4 + \dots)$
 $Q_K = Q_{ko}(b_0 + b_1 S + b_2 S^2 + b_3 S^3 + b_4 S^4 + \dots)$ (13)
 Where: $S = V/V_0$, and the active and reactive power components at the slack bus are then computed as:

$$P_S - jQ_S = \sum_{\substack{K=1 \\ K \neq S}}^N P_{SK} - jQ_{SK} \quad (14)$$

$$P_S - jQ_S = \sum_{\substack{K=1 \\ K \neq S}}^N E_S^* (E_S - E_K) Y_{SK}$$

Where: $P_S - jQ_S$ Load at the slack bus, Y_{SK} Mutual admittance between S and K nodes, and E_S Voltage at the slack bus.

From all the dynamic models involved in this type of simulation, the electric alternators is the one requiring more calculations since the rest of the dynamic components are given by a block diagram, in the initial condition calculations are made by simply setting to zero any term containing a derivative term [14]. Using the phasor diagram shown in Figure (3) the initial condition for the synchronous generator can be calculated as follows: At the terminal side of every alternator, the known variables given by a load-flow study, are: Terminal voltage $\bar{V}_a$, and Generated active power P , and reactive power Q .

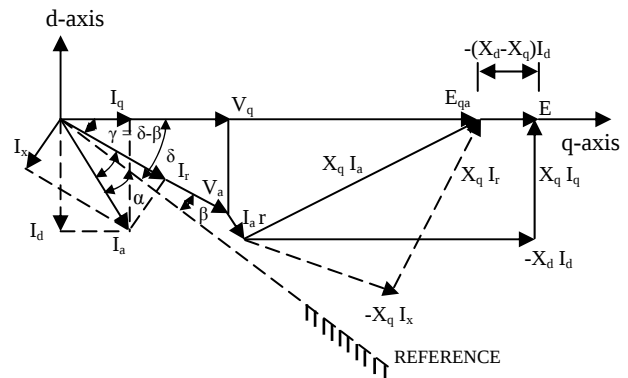


Figure 3. Phasor diagram of alternator initial condition.



Knowing these variables, it is easy to calculate the armature current of every unit and the corresponding power factor by:

$$\bar{I}_a = \left(\frac{P + jQ}{\bar{V}_a} \right)^* \quad (15)$$

Where: $\bar{I}_a$ is the armature current phasor, and Resolve $\bar{I}_a$ into components with $\bar{V}_a$ as a reference: $I_r = I_a \cos \phi$, $I_x = -I_a \sin \phi$, and the phasor $\bar{E}_{qa}$ in Figure (3), is given by:

$$\begin{aligned} \bar{E}_{qa} &= \bar{V}_a + (r + jx_q) \bar{I}_a \\ &= V_a + (I_r + jI_x)(r + jx_q) \\ &= (V_a - x_q I_x + r I_r) + j(x_q I_r + r I_x) \end{aligned} \quad (16)$$

Then the angle $(\delta - \beta)$ is given by:

$$\delta - \beta = \gamma = \frac{\arctan(I_x r + I_r x_q)}{(V_a - x_q I_x + r I_r)} \quad (17)$$

where angle δ is the position of the rotor. Then, calculate the terminal voltage d-q components:

$$V_d = -V_a \sin(\delta - \beta), V_q = V_a \cos(\delta - \beta) \quad (18)$$

Also, the armature current d-q components:

$$\begin{aligned} I_d &= -I_a \sin(\delta - \beta + \phi), I_q \\ &= I_a \cos(\delta - \beta + \phi) \end{aligned} \quad (19)$$

The field voltage of the machine from the stator side can be calculated by: $E = V_q + r I_q - x_d I_d = E_{FD}$. The initial position of the rotor is calculated by: $\delta = \gamma + \beta$. Calculate the initial value of the state variable of the alternator model using the quantities just calculated. Repeat the whole process for all the electric alternators present. Once the bus admittance matrix representing the network has been built and its entries organized as generator nodes, non-linear load nodes and other nodes, it is necessary to eliminate those nodes with zero current injections. The procedure to obtain the reduced network is as follows: Assume the Y_{BUS} matrix is partitioned in the following way:

$$\begin{bmatrix} I_G \\ I_L \\ 0 \end{bmatrix} = \begin{bmatrix} Y_{GG} & Y_{GL} & Y_{GR} \\ Y_{LG} & Y_{LL} & Y_{LR} \\ Y_{RG} & Y_{RL} & Y_{RR} \end{bmatrix} \begin{bmatrix} V_G \\ V_L \\ V_R \end{bmatrix} \quad (20)$$

Where: G means generator nodes, L means non-linear load nodes, R means remaining nodes.

Eliminate the remaining nodes by successive elimination procedure:

$$\begin{bmatrix} I_G \\ I_L \end{bmatrix} = \begin{bmatrix} Y'_{GG} & Y'_{GL} \\ Y'_{LG} & Y'_{LL} \end{bmatrix} \begin{bmatrix} V_G \\ V_L \end{bmatrix} \quad (21)$$

Where: $Y'_{GG} = Y_{GG} - Y_{GR} Y_{RR}^{-1} Y_{RG}$

$$Y'_{GL} = Y_{GL} - Y_{GR} Y_{RR}^{-1} Y_{RL}$$

$$Y'_{LG} = Y_{LG} - Y_{LR} Y_{RR}^{-1} Y_{RG}$$

$$Y'_{LL} = Y_{LL} - Y_{LR} Y_{RR}^{-1} Y_{RL}$$

After adding the internal impedance of each generator to new admittance matrix, the equation (21) becomes re-arranged as follows:

$$\begin{bmatrix} I_G \\ I_L \end{bmatrix} = \begin{bmatrix} Y''_{GG} & Y'_{GL} \\ Y'_{LG} & Y'_{LL} \end{bmatrix} \begin{bmatrix} E'_G \\ V_L \end{bmatrix} \quad (22)$$

Where just Y'_{GG} change to Y''_{GG} , and E'_G means the voltage behind the transient reactance. The equation (22) can be re-arranged as follows, this is because the initial load current at load buses are known, and it is used to determine the load voltage at load buses respectively at the next step.

$$\begin{bmatrix} I_G \\ V_L \end{bmatrix} = \begin{bmatrix} Y_{GG}^* & K_{GL} \\ H_{LG} & Z_{LL} \end{bmatrix} \begin{bmatrix} E'_G \\ I_L \end{bmatrix} \quad (23)$$

Where: Y_{GG}^* New admittance matrix, K_{GL}, H_{LG} Non-dimensional matrices, Z_{LL} Load impedance matrix. The equivalent matrix for the entire network is represented by the single line diagram of power system operating at the nominal loading condition is found as:

Where: $I_L = \frac{P_L - jQ_L}{V_L^*}$ (24)

$$\begin{aligned} P_L &= P_{ko}(a_0 + a_1 S_L + a_2 S_L^2 + a_3 S_L^3 + a_4 S_L^4 + \dots), \\ Q_L &= Q_{ko}(b_0 + b_1 S_L + b_2 S_L^2 + b_3 S_L^3 + b_4 S_L^4 + \dots) \end{aligned} \quad (25)$$

The system differential equations are depicts in Appendix.

3. Studied System

The power system used for digital simulation consists of multi-machine nine-bus system developed by WSCC in United States. A single line impedance diagram of the system is shown in Figure (4), where the system is basically composed of three generating units and three loads, load A, load B, and load C are located at buses # 4, # 5, and # 6, respectively. Unit one is hydroelectric, while units two and three are steam driven generators. The system is considered operating normally for 1sec. before fault is applied at # 5. The duration period which cause initiation of voltage instability in the considered system.



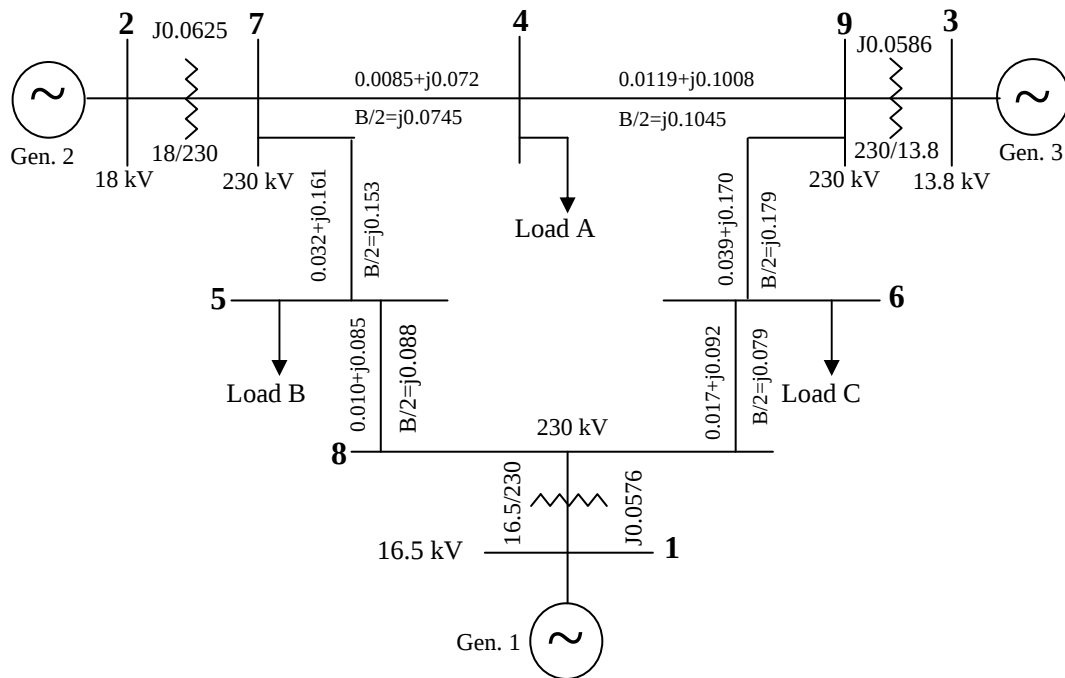


Figure 4. Studied system single line diagram

4. Cold Inrush Current

Cold inrush current is described as the magnitude of instantaneous current drawn by the load when startup the load, it's of short duration. Often milliseconds under a combination of certain conditions, when switching on solid state equipment such as computers, copiers, and electronic ballasts found in lighting system, as well as magnetic devices like motors, drives, core and coil ballasts, peak inrush current be several orders of magnitude greater than the circuit's steady state current will be drawn. It can be as high as 20 times normal operating current. Usually this inrush current is reduced after 10 ms, but could take up to 30 to 40 cycles until the current is at a normal value. Switching power supplies produce high inrush current at turn on resulting from filter capacitor impedance. These large filter capacitors act like a short circuit, producing an immediate inrush current with a fast rise time [18-22].

High inrush current can affect on electrical components such as tripping of circuit breakers as well as arcing and failure of primary circuit components, such as switches. High inrush currents also necessitate over sizing of fuses or breakers which complicates other aspects of approves testing and may compromise protection on other vital components. Another side effect of high inrush is the injection of noise and distortion back into the mains. During startup, momentary connect bouncing in switches or relay may cause the contacts to become pitted due to arcing between the contact points. This surge in current can also cause serious damage, such as welding switching contacts together. High inrush current may shorten life of switches and motion sensors and other devices which are used break contact in the electrical system. It can cause heavy demand of reactive power and voltage problems at

starting of network energizing. Starting of equipments and motors which require high starting currents than the normal values may suffer problems and failures.

The different types of load and their special inrush current are the two important factors to the switching frequency of the relay, in addition, they also greatly affect the contact welding, particularly the inrush current should be taken into consideration, and a range that is safety enough should be granted. From the available literature [19], the different types of load and their corresponding inrush current are given in Table (1), and Figure (5) indicates some forms of inrush current for some types of loads. Inrush current can be evaluated at many times of rated load current. High inrush current is a times-dependent phenomenon caused by a coincidental set of circumstances that must occur simultaneously at the moment of switch on [22]. Measuring this high startup current is important to determine the proper corrective equipment to install, such as surge limiters, soft start devices or simply increasing wire size, special equipments are required.

5. Cold Inrush Current Representation:

The cold inrush currents power factors are taken as (0.5, 0.6, 0.7, 0.8, and 0.9) pu. lagging or leading, and unity power factor. With each value of the load power factor, its recalculated. The inrush current has a value at the beginning and damp gradually to reach normal operating current. In our analysis, the inrush current decay takes the triangle form, as shown in Figure (6). Where: i_p is the peak inrush current ($i_p = m i_o$), i_o is the normal operating current, and t_p is the period of decay of the inrush current in seconds.



Table (1) Loads inrush currents periods and their ratio's to steady state values

Type of load	Inrush current / Rated current (i/i_o)	Period of inrush current
Resistive	Steady state current	0.07 to 0.1 second
Solenoid	10~20 times the steady state current	0.07 to 0.1 second
Motor	5~10 times the steady state current	0.2 to 0.5 second
Incandescent lamp	10~15 times the steady state current	Approx. 1/3 second
Mercury lamp	3 times the steady state current	
Sodium vapor lamp	1~3 times the steady state current	
Capacitive	20~40 times the steady state current	1/120 to 1/30 seconds
Transformer	5~10 times the steady state current	
Fluorescent lamp	5~10 times the steady state current	10 seconds or less
Electromagnetic contact	3 times the steady state current	1/60 to 1/30 seconds

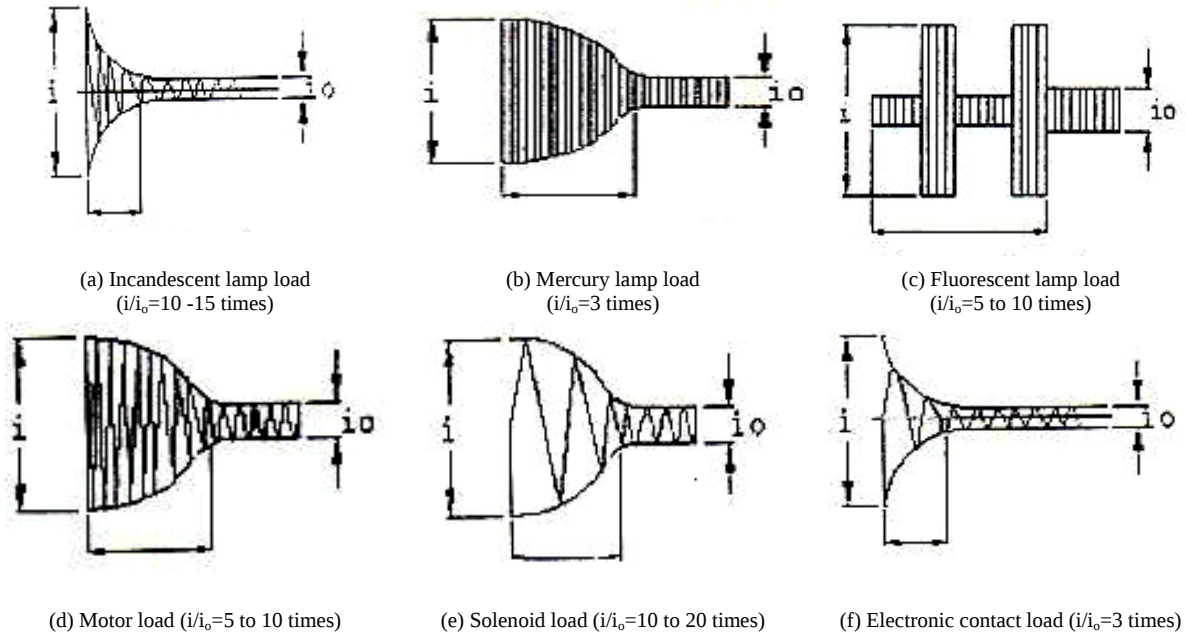


Figure 5. Some forms of inrush current for some types of loads

Indicates the inrush current decay and its magnitude at any instantaneous through this decay till it reaches to steady state current by equation $i=(t(i_o-mi_o)/t_p)+mi_o$, where: m is the peak inrush current times to its steady state value, t is the instantaneous time from (0 to t_p), and i is the instantaneous current.

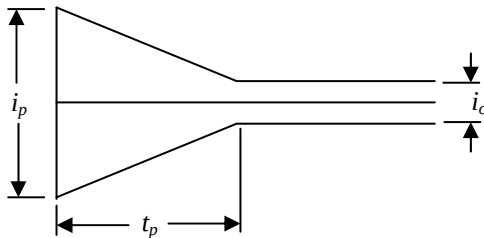


Figure 6. The form of inrush current in our analysis

6. Considered Disturbances

The considered disturbance is suddenly increasing of the peak inrush current many times of normal operating system current period of time at load bus # 5, with lagging and leading loads power factors at these buses successively, until the voltage instability starts to occur.

The loads are represented by CZ loads at all load buses. The other disturbance, the time response for load buses voltages at load buses with varying the peak inrush current by many times of normal operating current such as (100%, 300%, and 500%) of normal operating current at load bus #5, at $t=1$ sec., for a duration period equal 0.2 sec., and opening the line connecting between two buses #5 and #7 at $t=5$ sec. until the voltage collapse happen, with lagging and leading loads power factors such as (0.8 pu.). The system is let to reach steady-state operating condition opening at $t=5$ sec. The fault is taken as opening the line which connects between nodes #5 and #7 and lasts until state of voltage collapse initiation is attained.

7. Results and Discussions

7.1 Effect of suddenly increasing of the peak inrush current on the duration period for voltage instability:

The duration period of inrush current which cause voltage instability when the inrush current suddenly increases, where it takes different initial peaks such as (16, 15, and 12) times of normal operating system current, at bus (5). Load power factors at load bus (5) is



taken as (0.5, 0.6, 0.7, 0.8, and 0.9) pu. lagging or leading. It can be noticed that, the duration period of suddenly increases of inrush current which cause voltage instability initiation increases by decreasing the value of the peak inrush current for each value of lagging, leading and unity load power factor at load bus (5), also it can be observed that, the duration period of suddenly increases of inrush current which cause voltage instability initiation increases by increasing the values of lagging power factors at load bus (5), and the duration period of suddenly increases of inrush current which cause initiation of voltage instability decreases by increasing the values of leading power factors at load bus (5) for the same value of peak inrush current, as shown in Figure 7 (a, b, and c) for load power factors (0.5, 0.7, 0.9) pu. (lagging and leading), respectively.

Also, Figure 8 (a, b) shows voltage stability limit, and indicate the zone of the unstable voltage and that of stable region with suddenly increasing of the peak inrush current by (12, 15, and 16) times of normal operating current system, with lagging and leading power factor respectively, at load bus (5) from 0.5 pu. to unity. Where the region of stability increases by decreasing the value of peak inrush current at any load power factor lagging or leading, and indicate that the duration period of suddenly increasing an inrush current which cause voltage instability initiation increases from lagging to leading loads power factor.

7.2 Time response for loads voltages with different times of normal operating current system, due to suddenly increasing of (i/i_o):

The time response for load buses voltages at load buses with varying the suddenly increasing of the peak inrush current by many times of normal operating current system such as (16, and 12) times at load bus (5) as a disturbance, with lagging and leading load power factors such as (0.7 pu. Figure 9 indicates that, the time response for load buses voltages with suddenly increasing the value of peak inrush current of (16, and 12) times of normal operating current system, with 0.7 pu. lagging power factor at load bus (5). with the value of peak inrush current is 16 times of normal operating current, when the duration period is 200 msec., the system is stable and when the duration period is 201 msec., the system starts to be unstable, as shown in Figure 9(a, b), respectively. While, with the value of peak inrush current of 12 times the normal operating current, the duration period is 239 msec., and system voltage is stable, while when the duration period is 240 msec., the system voltage become unstable, as shown in Figure 9(c, d), respectively.

Figure 10, we can observe that, the time response for load buses voltages with suddenly increasing the value of peak inrush current of (16, and 12) times of normal operating current, and with 0.7 pu. leading power factor at load bus (5), the duration period of suddenly increases of inrush current is 271 msec., and the system voltage is stable, while when the duration period of suddenly increases of inrush current is 272 msec., the system voltage starts to be unstable, as shown in Figure 10(a, b),

respectively. While, with suddenly increasing the value of peak inrush current of 12 times of normal operating current, the duration period is 488 msec., and the system voltage is stable, while when the duration period is 489 msec., the system voltage become unstable, as shown in Figure 10(c, d), respectively. Where: T_c refers to duration period of inrush current.

7.3 Voltage stability limits due to opening the line between with peak inrush current at load bus by (100%, 300%, and 500%) of normal operating current system:

Figure 11 indicate the fault duration which cause initiation of voltage collapse when opening the line connecting between two buses (5) and (7) at $t=5$ sec., with the peak inrush current to normal load current is (100%, 200%, 300%, 400%, and 500%) for a period of time equal 0.2 sec. at $t=1$ sec., at bus (5), load buses power factors are taken as (0.6, 0.7, 0.9) pu. lagging or leading power factors. All load buses are constant impedance loads. Also, the fault duration which cause voltage collapse decreases by increasing the peak inrush current for each value of lagging, leading and unity loads power factor at load bus (5), for the same peak inrush current. The fault duration which cause voltage collapse increases by increasing the lagging loads power factor towards unity power factor. But, it decreases slightly by increasing the leading loads power factor at load bus (5), as shown in Figure 11 (a, and b) for loads power factors (0.6, 0.7, 0.9) pu. (lagging and leading), respectively. Once more the decrease of the fault duration period is very slight with leading power factors. This is the same with higher lagging power factor loads.

Figure 12 (a, b) shows voltage stability limit, and indicate the zone of the unstable voltage and that of stable region when opening the line between two buses (5) and (7) after the system reach to steady state operating condition, but in the beginning, the system suffer from increasing the peak inrush current of (100%, 300%, and 500%) of the normal operating current system can take a period as 0.2 sec., with lagging and leading loads power factor, respectively at load bus (5) from 0.5 pu. to unity. Where the region of stability increases by decreasing the value of peak inrush current at any load power factor lagging or leading, and indicate that the fault duration period which cause voltage collapse initiation increases from lagging to leading loads power factor.

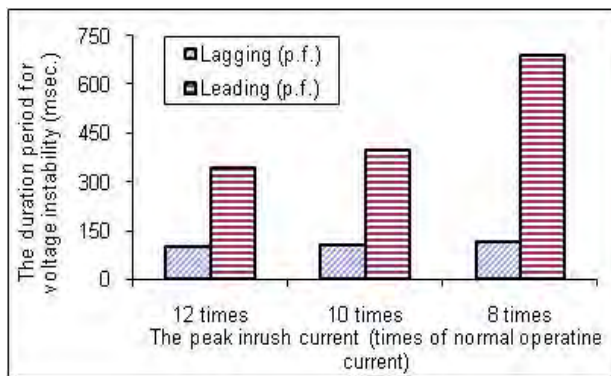
7.4 Time response for loads voltages, with line opening, when (i/i_o) at load bus # 5, at 100%, 300%, and 500%:

This section, shows that, the time response for load buses voltages at load buses with varying the peak inrush current by many times of normal operating current such as (100%, 300%, and 500%) of normal operating current at load bus (5), at $t=1$ sec., for a duration period equal 0.2 sec., and opening the line connecting between two buses (5) and (7) at $t=5$ sec. until the voltage collapse happen, with lagging and leading loads power factors such as (0.8 pu.). Figure 13 indicates that, the time response for the load buses voltages with the peak inrush current is (100%, 300%, and 500%) of the normal operating

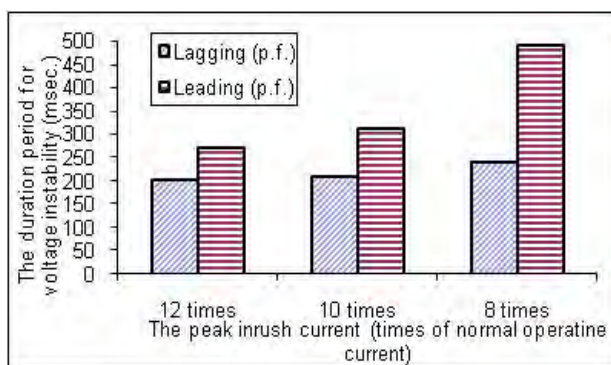


current, with 0.8 pu. lagging power factor at load bus (5), where, without inrush current (100% of the normal operating current), the fault duration which cause voltage collapse is 13.46 sec.. When peak inrush current is (300%), the fault duration which cause voltage collapse becomes 12.08 sec., and when the peak inrush current is (500%), the fault duration which cause voltage collapse goes to 10.92 sec., as shown in Figure 13(a, b and c), respectively.

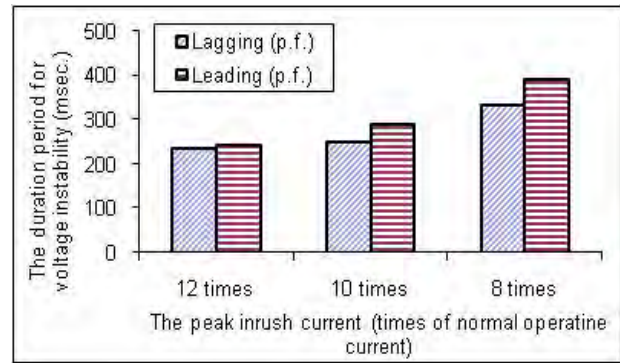
Figure 14 indicates the time responses load buses voltages with peak inrush currents (100%, 300%, and 500%) of normal operating current, with 0.8 p.u. leading power factor at load bus (5), where, without inrush current (100% of normal operating current), the fault duration which cause voltage collapse is 22.02 sec., while when the peak inrush current is (300%) of normal operating current, the fault duration which cause voltage collapse becomes 21.16 sec.. For inrush current of (500%), the fault duration which cause voltage collapse turns to be 19.88 sec., as shown in Figure 14 (a, b and c), respectively.



(a) power factor = 0.5 pu

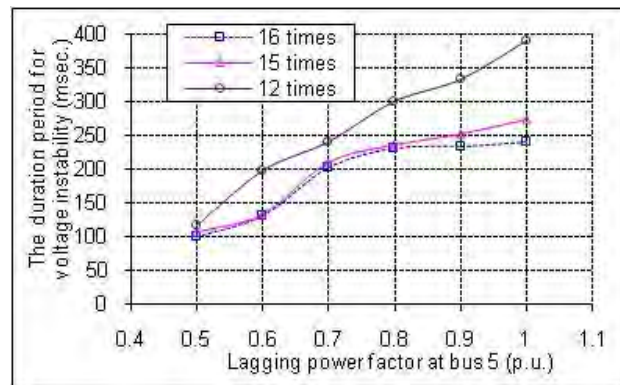


(b) power factor = 0.7 pu

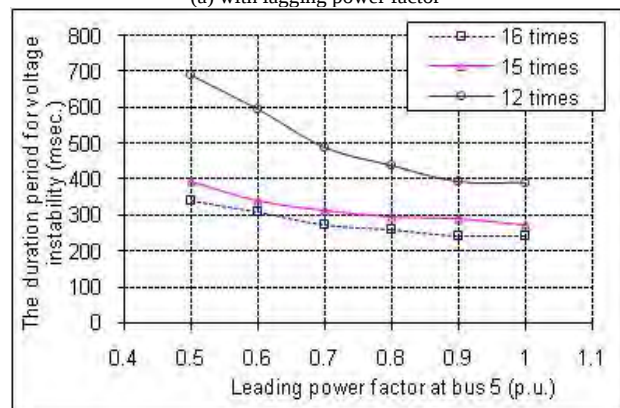


(c) power factor = 0.9 pu

Figure 7. Effect of suddenly increasing of the peak inrush current many times at load bus (5) on the duration period for voltage instability, with lagging and leading power factor

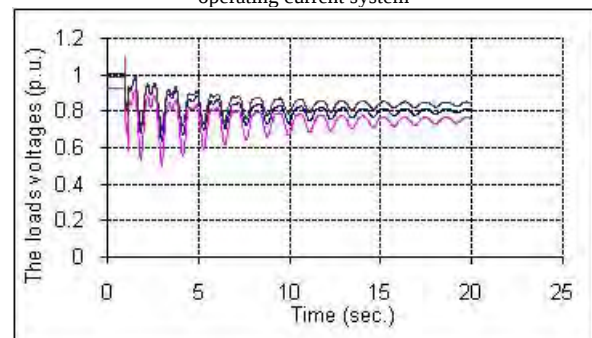


(a) with lagging power factor



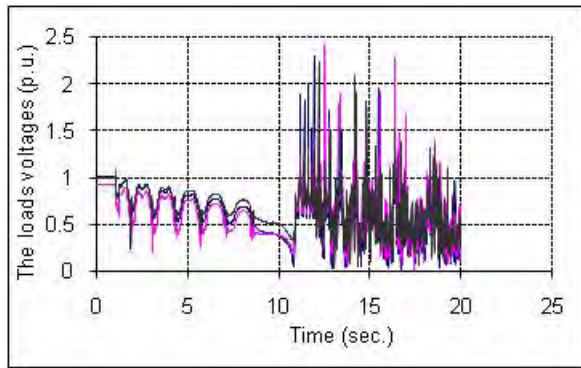
(b) with leading power factor

Figure 8. Voltage stability limit, due to suddenly increasing of peak inrush current at load bus (5) by (12, 15, and 16) times of normal operating current system

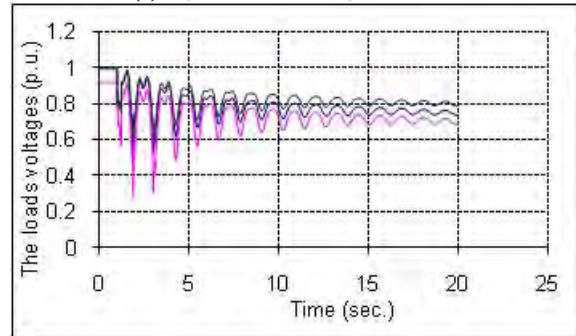


(a) $i/i_0 = 16$ times, and $T_c = 200$ msec.

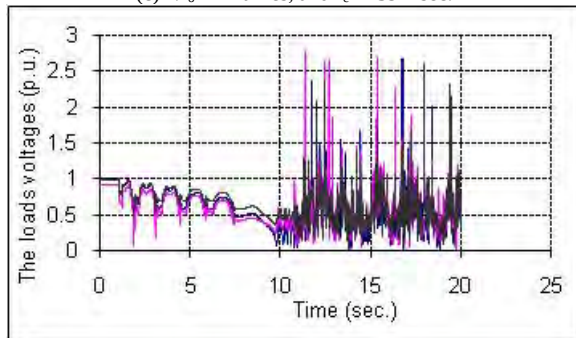




(b) $i/i_o = 16$ times, and $T_c = 201$ msec.

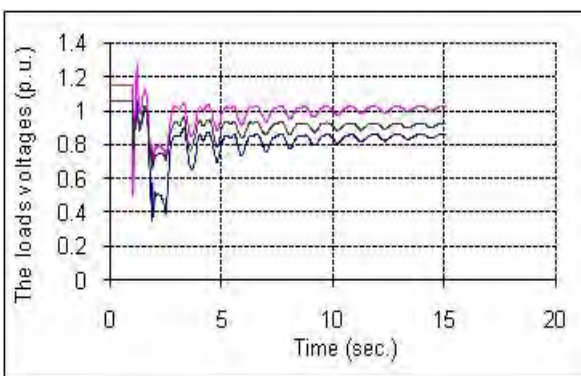


(c) $i/i_o = 12$ times, and $T_c = 239$ msec.

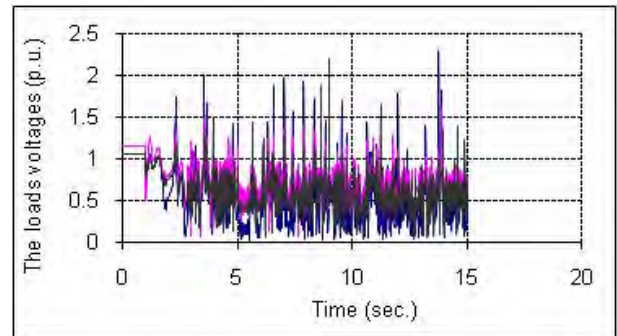


(d) $i/i_o = 12$ times, and $T_c = 240$ msec.

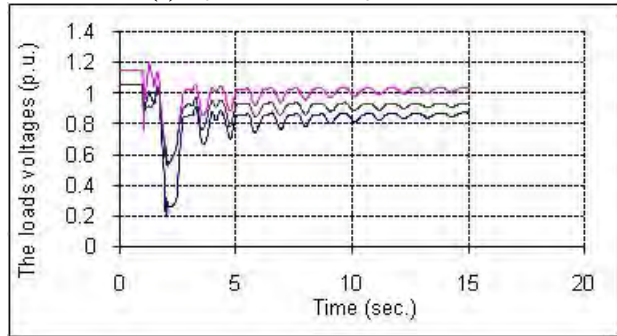
Figure 9. Time response for loads voltages, with 0.7 pu. lagging pf., due to suddenly increasing of (i/i_o) at load bus (5)



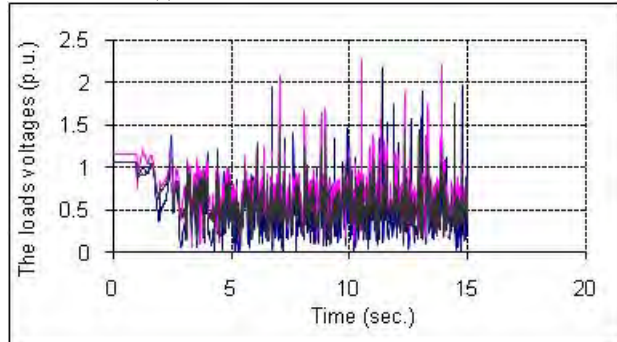
(a) $i/i_o = 16$ times, and $T_c = 271$ msec.



(b) $i/i_o = 16$ times, and $T_c = 272$ msec.

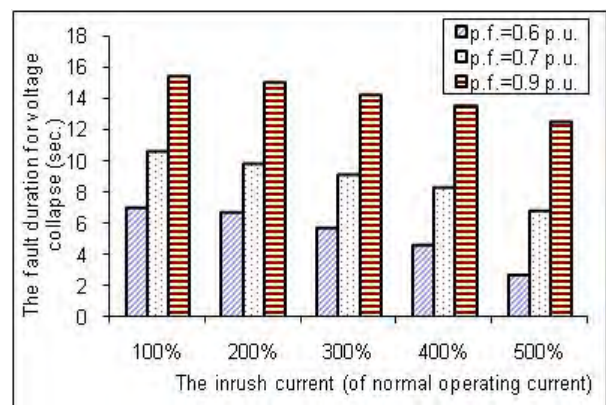


(c) $i/i_o = 12$ times, and $T_c = 488$ msec.



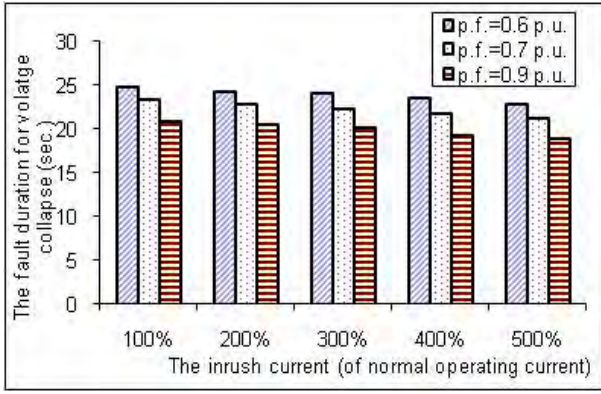
(d) $i/i_o = 12$ times, and $T_c = 489$ msec.

Figure 10. Time response for loads voltages, with 0.7 pu. leading pf., due to suddenly increasing of (i/i_o) at load bus (5)



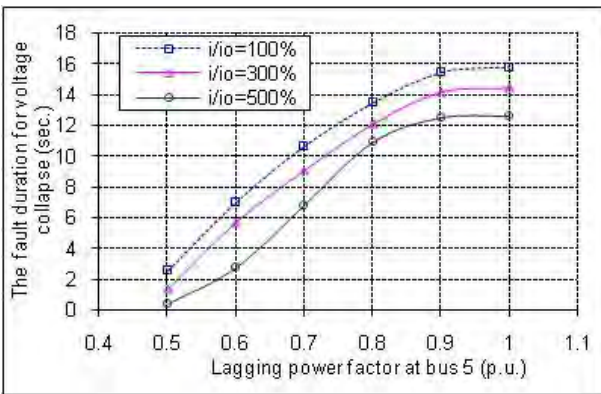
(a) with lagging power factor



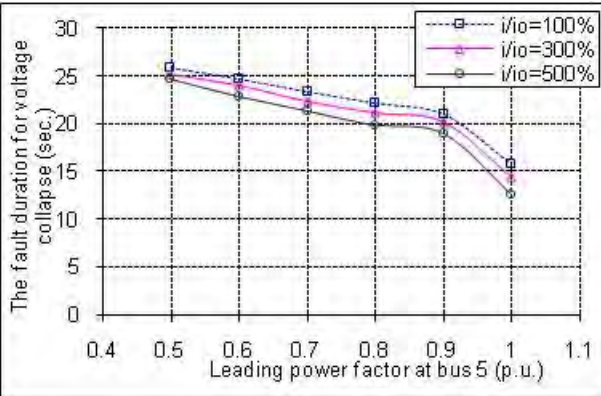


(b) with leading power factor

Figure 11. Effect of peak inrush current wrt nominal current at load bus (5) on the fault duration for voltage collapse, when opening the line connecting between two buses (5) and (7),



(a) with lagging power factor



b) with leading power factor

Figure 12. Voltage stability limit, due to opening the line between two nodes (5) and (7), with peak inrush current at load bus (5) by (100%, 300%, and 500%) of normal operating current system a period 0.2 sec.,

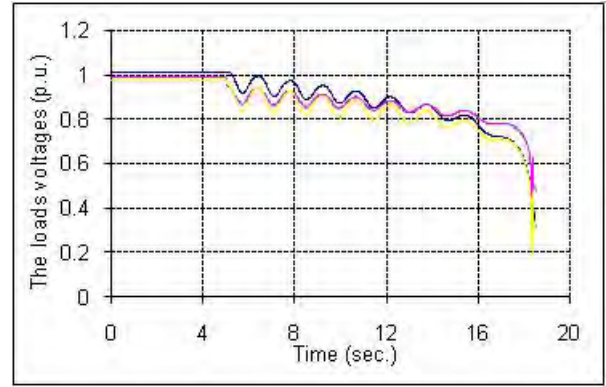
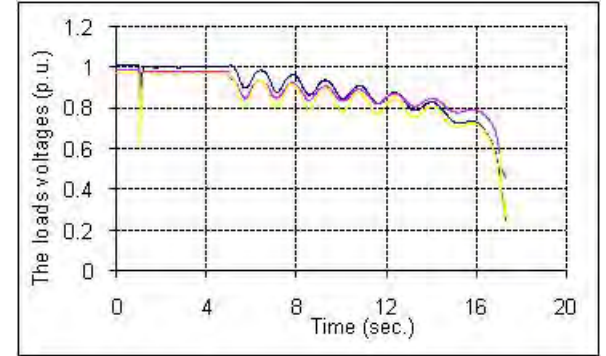
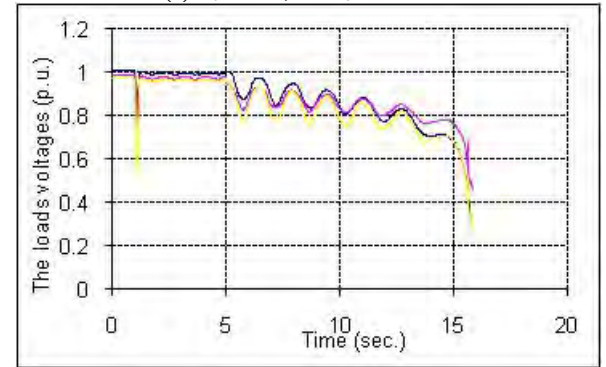
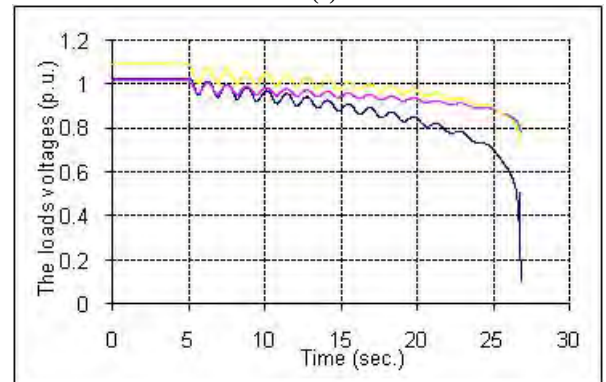

(a) $i/i_o = 100\%$, and $T_c = 13.46$ sec.

(b) $i/i_o = 300\%$, and $T_c = 12.08$ sec.

(c) $i/i_o = 500\%$, and $T_c = 10.92$ sec.

Figure 13. Time response for loads voltages, with 0.8 pu. lagging power factor, with line between nodes (5) and (7) opening, when (i/i_o) at load bus (5)

(a) $i/i_o = 100\%$, and $T_c = 22.02$ sec.

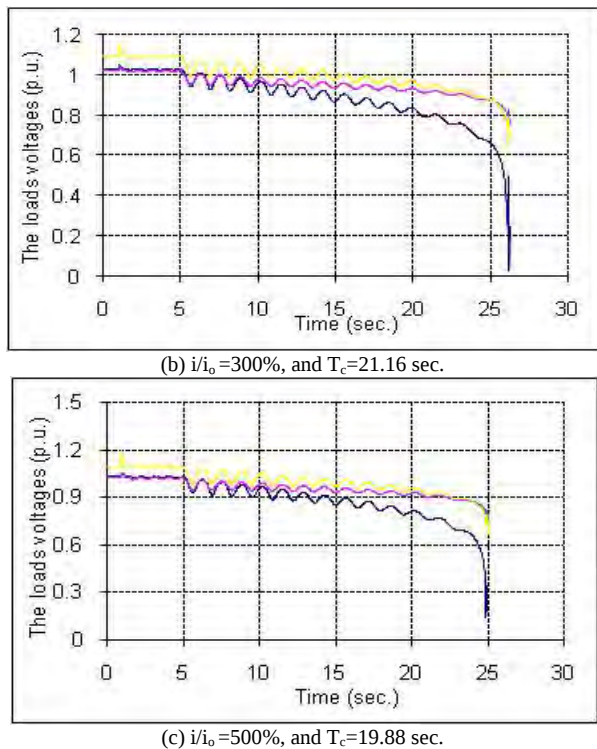



Figure 14. Time response for loads voltages, with 0.8 pu. leading power factor, with line between nodes (5) and (7) opening, when (i/i_0) at load bus (5)

8. Conclusions

We can conclude that the voltage instability is influenced by cold inrush current application period: The duration period of increasing peak inrush current suddenly which cause voltage instability initiation increases by decreasing the value of peak inrush current, this is true with lagging and leading loads power factors. The duration period of increasing peak inrush current suddenly which cause initiation of voltage instability increases by increasing the loads power factor (in the lagging rang) for the same value of the peak inrush current. Voltage instability initiation decreases by increasing the leading loads power factor for the same value of the peak inrush current.

The fault duration which cause initiation of voltage collapse is affected by the cold inrush current magnitudes, and the load power factors either in the leading or in the lagging ranges. For a sudden line opening, the fault duration period which cause voltage collapse initiation increases by decreasing the peak inrush current magnitudes for each value of lagging, leading and unity loads power factors. The fault duration which cause initiation of voltage collapse increases by increasing the lagging loads power factor, for the same value of the peak inrush current. While, the fault duration period which cause voltage collapse decreases by increasing the value of leading loads power factor for the same value of the peak inrush current magnitudes.

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Appendix

System Differential Equations:

$$[g([x], [y])] = 0 \quad (1)$$

$$\begin{bmatrix} \dot{x} \\ \dot{y} \end{bmatrix} = [f([x], [y])] \quad (2)$$

Network General Features:

$$\begin{bmatrix} I_G \\ V_L \end{bmatrix} = \begin{bmatrix} Y_{GG} & K_{GL} \\ H_{LG} & Z_{LL} \end{bmatrix} \begin{bmatrix} V_G \\ I_L \end{bmatrix} \quad (3)$$

$$\begin{bmatrix} V_Q \\ V_D \end{bmatrix} = \begin{bmatrix} \cos \theta & -\sin \theta \\ \sin \theta & \cos \theta \end{bmatrix} \begin{bmatrix} v_q \\ v_d \end{bmatrix} \quad (4)$$

$$\begin{bmatrix} v_q \\ v_d \end{bmatrix} = \begin{bmatrix} \cos \theta & \sin \theta \\ -\sin \theta & \cos \theta \end{bmatrix} \begin{bmatrix} V_Q \\ V_D \end{bmatrix} \quad (5)$$

Steady-state equations solution:

$$\begin{bmatrix} v_q \\ v_d \end{bmatrix} = \begin{bmatrix} E'_q \\ E'_d \end{bmatrix} - \begin{bmatrix} R_a & -x'_d \\ x'_q & R_a \end{bmatrix} \begin{bmatrix} I_q \\ I_d \end{bmatrix} \quad (6)$$

$$I = I_{real} + jI_{imag}, \text{ and } I_q + jI_d = Ie^{-j\theta} \quad (7)$$

$$v_q + jv_d = (E'_q + jE'_d) - (R_a + jx'_d)(I_q + jI_d) \quad (8)$$

$$V = E' - (R_a + jx'_d)I \quad (9)$$

$$\begin{bmatrix} V_{real} \\ V_{imag} \end{bmatrix} = \begin{bmatrix} E'_{real} \\ E'_{imag} \end{bmatrix} - \begin{bmatrix} \cos \theta & -\sin \theta \\ \sin \theta & \cos \theta \end{bmatrix} \begin{bmatrix} R_a & -x'_d \\ x'_q & R_a \end{bmatrix} \begin{bmatrix} I_{real} \\ I_{imag} \end{bmatrix} \quad (10)$$

$$Y^{fict} = \frac{R_a - j\frac{1}{2}(x'_d + x'_q)}{R_a^2 + x'_d x'_q} \quad (11)$$

$$E^{fict} = E' + \frac{j\frac{1}{2}(x'_q - x'_d)}{R_a - j\frac{1}{2}(x'_d + x'_q)}(E'^* - V^*)e^{j2\theta} \quad (12)$$

$$\begin{bmatrix} I_G \\ I_L \\ 0 \end{bmatrix} = \begin{bmatrix} Y_{GG} & Y_{GL} & Y_{GR} \\ Y_{LG} & Y_{LL} & Y_{LR} \\ Y_{RG} & Y_{RL} & Y_{RR} \end{bmatrix} \begin{bmatrix} E^{fict} \\ V_L \\ V_R \end{bmatrix} \quad (13)$$

$$\begin{bmatrix} I_G \\ V_L \end{bmatrix} = \begin{bmatrix} Y & H \\ K & Z \end{bmatrix} \begin{bmatrix} E^{fict} \\ I_L \end{bmatrix} \quad (14)$$

Biographies



Youssef A. Mobarak was born in Egypt. He received his B.Sc. and M.Sc. degrees in Electrical Engineering from South Valley University, Aswan, Egypt, in 1997 and 2001 respectively and Ph.D. from Cairo University, Egypt, in 2005. He joined Electrical Engineering Department, High Institute of Energy, South Valley University as a Demonstrator, as an Assistant Lecturer, and as an Assistant Professor during the periods of 1998–2001, 2001–2005, and 2005–2009 respectively. He joined Artificial Complex Systems, Hiroshima University, Japan as a Researcher 2007–2008. Also, he joined King Abdul-Aziz University, Rabigh, Faculty of Engineering 2010 to present. His research interests are power system planning, operation, and optimization techniques applied to power systems, Nanotechnology materials via addition nano-scale particles and additives for usage in industrial field.



Mahmoud M. Hussein was born in Egypt. He received his B.Sc. and M.Sc. degrees in Electrical Engineering from South Valley University, Aswan, Egypt, in 2000 and 2006 respectively. He is currently a Ph.D. student in Minia University, Egypt, from 2008. He joined Electrical Engineering Department, High Institute of Energy, South Valley University as a Demonstrator, and as an Assistant Lecturer during the periods of 2002–2006, 2006–2008. He joined Aswan Power Electronics Applications Research Center, South Valley University as an Assistant Researcher 2009–2011, Also, he is joining Graduation School of Engineering and Science, Ryukyus University, Okinawa, Japan, as a Researcher 2011 to present. His special fields of interests include distributed generation, renewable energy, power system planning, power quality, and reliability.





Novel Controllers for Reactive Power Compensation by using STATCOM

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Abstract

This paper proposes and validates models to accurately represent Static Synchronous Compensators STATCOM regulates system voltage in transient (load injection and rejection) by absorbing or generating reactive power of sample three-bus test system. The proposed STATCOM models are justified based on the basic operational characteristics of this Flexible AC Transmission System FACTS controller for both phase and PWM control strategies. The complete digital simulation of the STATCOM within the power system is performed in the MATLAB/Simulink environment using the Power System Blockset PSB. The performance of STATCOM scheme connected to the 230kV grid is evaluated. In addition, all details of the models implementation, the controls used, and the data for the test system are providing in the paper.

Keywords: *FACTS, STATCOM, Reactive Power Control.*

1. Introduction

The STATCOM is the state of the art technology for dynamic control of reactive power. A STATCOM can be used for steady-state voltage control, to increase the stability of generator systems during transient grid fault and for dynamic reactive power generation. The STATCOM technology is based on a Voltage Source Converter connected by a filter and possibly a coupling transformer in shunt to the power system. A detailed review on literature about the STATCOM technology is given in [1]. Compared to the Static Var Compensator SVC, which consists of a thyristor controlled reactor, the STATCOM is often the preferred solution due to its dynamic reactive power generation capabilities, the smaller size and the ability to generate reactive power during low grid voltage condition.

The commercial availability of gate turn-off thyristor (GTO) switching devices with high power handling capability, and the advancement of the other types of power semiconductor devices, such as IGBT's have led to the development of fast controllable reactive

power sources utilizing new electronic switching and converter technology. These switching technologies additionally offer considerable advantages over existing methods in terms of space reductions and fast effective damping [1]. Benefits provided by FACTS devices are twofold: they are capable of increasing power transfer over transmission lines and can make these power transfers fully controllable [2]. The last decade has witnessed a wide interest in design and application of FACTS devices, both from academic and industrial research. Several enhancement in semiconductor technologies and manufacturing have made power electronic devices capable of handling high power levels GTO, IGBT, IGCT commercially available [3]. Many devices have been proposed in the past within the FACTS family, some of them based on thyristor switched reactors like the Thyristor Controlled Series Compensator TCSC and the SVC [4], the Static Synchronous Series Compensator SSSC [5], the Unified Power Flow Controller UPFC [6] and the Superconducting. Magnetic Energy Storage systems SMES [7], employ power electronic converters. While others, like the STATCOM present in this paper.

The need for more secure and efficient electricity supply systems management has given rise to innovative technologies in power generation and transmission. The combined cycle power station is a good example of a new development in power generation and FACTS as they generally known, are new devices that improve transmission systems [8-11]. In addition, the economical utilization of transmission system assets is of vital importance to enable electric utilities in industrialized countries to remain competitive and to survive. In developing countries, the optimized use of transmission systems investments is also very important to support industry, create employment and utilize efficiently scarce economic and industrial resources. FACTS is the new engineering technology that responds to these needs. It significantly alters the way power transmission systems are developed and controlled together with improvement in asset utilization, power system flexibility and effective system performance [12-17].



Especially for the grid integration of wind farms the STATCOM technology has gained a lot of attention in the last years. The increased amount of renewable energy sources connected to the power system has forced transmission system operators to tighten their grid code requirements. A detailed overview on grid codes is given in [18]. A STATCOM for the grid integration of wind power plants in a weak loop power system can be found in [19]. In [20] and [21] the STATCOM is compared with a SVC to increase the stability of fixed speed induction generator wind turbines during grid voltage dips. By compensating the high amount of reactive power drawn by these generators after they have accelerated during a grid voltage dip can significantly increase the critical clearing time which is a criteria for the stability of the system. To perform an indirect torque control for the same kind of generators during grid voltage dips, the STATCOM is investigated in [22]. It can be shown that the torque of the machines can indirectly be controlled by the reactive power to reduce stress on the generator shaft to increase the gearbox lifetime.

The coordinated control of a STATCOM and a wind farm with doubly-fed induction generators is shown in [23]. Most of these studies focus on the behaviour during three-phase grid voltage dips. This matches the requirements given by the grid codes, that often only consider balanced grid voltage conditions and only give short comments on the requirements during unbalanced grid voltage dips. But, the majority of grid faults are of unbalanced nature [24]. Such a negative sequence voltage can lead to undesired oscillations in current, STATCOM reactive and active power and DC link voltage and can make the STATCOM tripping, which should be avoided under all circumstances for power system stability reasons. Thus, safe control structures that can handle even significant voltage unbalances should be applied to the STATCOM.

A STATCOM controller for handling unbalanced grid voltage with positive and negative sequence control loops can be found in [25]. Furthermore, the STATCOM currents can be injected into the power system aiming for different targets. The STATCOM can inject a purely positive sequence current or it can also inject a negative sequence current, which has been done in [26]. In [27] three control strategies under a given current rating of the converter are introduced.

This paper proposes and validates models to accurately represent STATCOM regulates system voltage in transient by absorbing or generating reactive power of sample three-bus test system. The performance of STATCOM scheme connected to the 230kV grid is evaluated. In addition, all details of the models implementation, the controls used, and the data for the test system are providing in the paper.

2. STATCOM Model

2.1 STATCOM Equivalent Circuit

The STATCOM is a shunt device of the FACTS family using power electronics to control power flow and improve transient stability on power grids. The STATCOM regulates voltage at its terminal by controlling the amount of reactive power injected into or absorbed from the power system. The variation of reactive power is performed by means of a Voltage Sourced Converter VSC connected on the secondary side of a coupling transformer. The principle of operation of the STATCOM is explained on the Fig. (1). The active and reactive power transfer between a source V_1 and a source V_2 can be calculated by:

$$P = \frac{V_1 V_2 \sin \delta}{X} \quad (1)$$

Where, V_1 represents the system voltage to be controlled and V_2 is the voltage generated by the VSC.

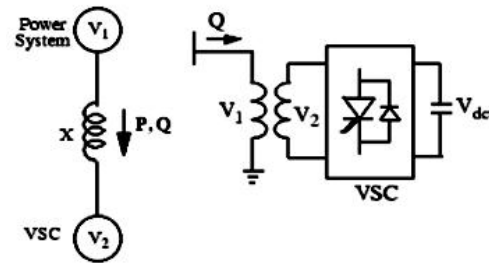


Figure 1. The active and reactive power transfer between sources

In steady state operation, the voltage V_2 generated by the VSC is in phase with V_1 ($\delta=0$), so that only reactive power is flowing ($P=0$). If V_2 is lower than V_1 , the reactive power Q is flowing from V_1 to V_2 (STATCOM is absorbing reactive power). While, if V_2 is higher than V_1 , the reactive power Q is flowing from V_2 to V_1 (STATCOM is generating reactive power). The amount of reactive power is given by:

$$Q = \frac{V_1(V_1 - V_2)}{X} \quad (2)$$

A capacitor connected on the dc side of the VSC acts as a dc voltage source. In steady state the voltage V_2 has to be phase shifted slightly behind V_1 in order to compensate for transformer and VSC losses and to keep the capacitor charged. Two VSC technologies can be used for the VSC: The first, VSC using GTO based square wave inverters and special interconnection transformers. In this type of VSC, the fundamental component of voltage V_2 is proportional to the voltage V_{dc} . Therefore, V_{dc} has to be varied for controlling the reactive power. The second, VSC using IGBT based PWM inverters. This type of inverter uses PWM technique to synthesize a sinusoidal waveform from a dc voltage source with a typical chopping frequency of a few kilohertz. This type of VSC uses a fixed dc voltage V_{dc} . Voltage V_2 is varied by changing the modulation index of the PWM modulator.



2.2 STATCOM Controls Circuit

The control system consists of: A PLL which synchronizes on the three-phase primary voltage V_1 , and the output of the PLL (angle $\theta = \omega t$) is used to compute the direct-axis and quadrature-axis components of the ac three-phase voltage and currents labeled as V_d , V_q or I_d , I_q on the Fig. (2). Measurement systems measuring the d and q components of ac voltage and currents to be controlled as well as the dc voltage V_{dc} . An outer regulation loop consisting of an ac voltage regulator and a dc voltage regulator. Where: V is a positive sequence voltage, I is a reactive current, and X_s is a slope or droop reactance. The output of the ac voltage regulator is the reference current I_{qref} for the current regulator.

The output of the dc voltage regulator is the reference current I_{dref} for the current regulator. The current regulator controls the magnitude and phase of the voltage generated by the PWM converter (V_{2d} , V_{2q}) from the I_{dref} and I_{qref} reference currents produced respectively by the dc voltage regulator and the ac voltage regulator. The current regulator is assisted by a feed forward type regulator which predicts the V_2 voltage output (V_{2d} , V_{2q}) from the V_1 measurement (V_{1d} , V_{1q}) and the transformer leakage reactance.

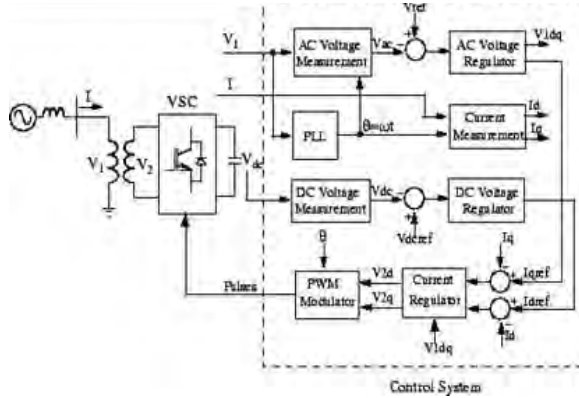


Figure 2. STATCOM and its control system

The STATCOM can be operated in two different modes: In voltage regulation mode, and other, in var control mode. When the STATCOM is operated in voltage regulation mode, it implements the following V/I characteristic as shown in Fig. (3).

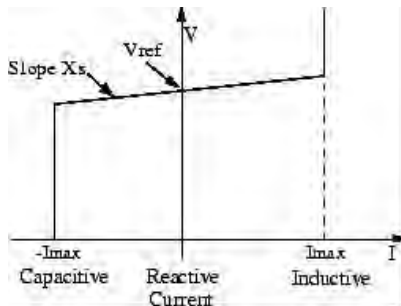


Figure 3. STATCOM $V-I$ characteristic

As long as the reactive current stays within the minimum and maximum current values ($-I_{max}$, I_{max}) imposed by the converter rating, the voltage is regulated at the reference voltage V_{ref} . However, a voltage droop is normally used (usually between 1% and 4% at maximum reactive power output). In the voltage regulation mode, the V/I characteristic is described by the following equation:

$$V = V_{ref} - X_s I \quad (3)$$

Figure (4) shows the basic control structure of the STATCOM, based on a decoupled, $d-q$ controlled strategy and the two levels, 24-pulse converter. The regulation slope k is defined as:

$$k = \frac{\Delta V_{cmax}}{I_{cmax}} = \frac{\Delta V_{lmax}}{I_{lmax}} \quad (4)$$

Where ΔV_{cmax} is the maximum line voltage drop at full capacitive and ΔV_{lmax} is the maximum line over voltage at full inductive operation of the STATCOM. I_{cmax} and I_{lmax} are the corresponding STATCOM current ratings.

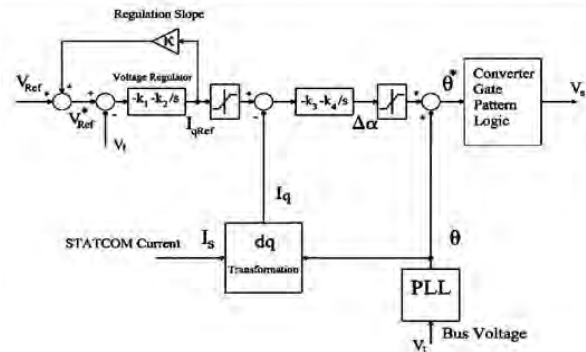


Figure 4. Basic control structure of the STATCOM

2.3 Dynamic Representation of the STATCOM

A power system representation as seen by the STATCOM (including coupling transformer), and X_{eq} is the equivalent Thevenin reactance of the power system as seen by the STATCOM, V_{eq} is the equivalent Thevenin voltage and V_t is the STATCOM bus voltage. For the STATCOM capacitive operation we have:

$$V_t = V_{eq} + I_q X_{eq} \quad (5)$$

Figure (5) shows the functional block diagram representation of the STATCOM when operating in capacitive mode. The STATCOM can be modeled as a reactive current source with transportation lag, T_q . The voltage regulator and the feedback voltage measurement are also modeled with transportation lags of T_1 , and T_2 , respectively.

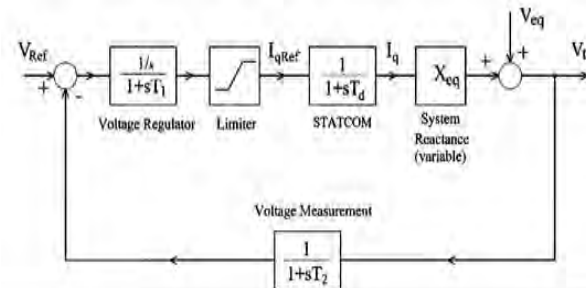


Figure 5. Block diagram representation of the STATCOM



2.4 STATCOM Mathematical Equations

The differential equations for a STATCOM summarized are:

$$\frac{di_{as}}{dt} = -(\omega_o/x_1)(Ri_{as} - (v_{as}^i - v_{as})) \quad (6)$$

$$\frac{di_{bs}}{dt} = -(\omega_o/x_1)(Ri_{bs} - (v_{bs}^i - v_{bs})) \quad (7)$$

$$\frac{di_{cs}}{dt} = -(\omega_o/x_1)(Ri_{cs} - (v_{cs}^i - v_{cs})) \quad (8)$$

$$\frac{dv_{DC}}{dt} = -\frac{\omega_o}{b_c} \left(\frac{v_{DC}}{R_p} + i_{DC} \right) \quad (9)$$

Where: i_{DC} and $v_{as}^i, v_{bs}^i, v_{cs}^i$ are the dc side current and converter output phase voltages respectively. Now, $(i_{as} + i_{bs} + i_{cs}) = 0$ and if we assume $(v_{as} + v_{bs} + v_{cs}) = 0$ then it implies that $(v_{as}^i + v_{bs}^i + v_{cs}^i) = 0$.

Then we can write the converter output voltages as:

$$v_{as}^i = v_{DC} S_6^a(t) \quad (10)$$

$$v_{bs}^i = v_{DC} S_6^b(t) \quad (11)$$

$$v_{cs}^i = v_{DC} S_6^c(t) \quad (12)$$

Where S_6^a, S_6^b, S_6^c are the switching function for a six-pulse STATCOM, In the steady state:

$$S_6^a(t) = S_6^b\left(t + \frac{2\pi}{\omega_0 3}\right) = S_6^c\left(t + \frac{4\pi}{\omega_0 3}\right) \quad (13)$$

If we assume the converter to be lossless,

$$v_{DC} i_{DC} = v_{as}^i i_{as} + v_{bs}^i i_{bs} + v_{cs}^i i_{cs} \quad (14)$$

Therefore, by substitute equation (11) to (13) into equation (14)

$$i_{DC} = i_{as} S_6^a + i_{bs} S_6^b + i_{cs} S_6^c \quad (15)$$

Clearly, the dynamical equation are time-variant and difficult to analysis for a varying firing angle. If the switching function S_6^a, S_6^b, S_6^c are approximated by their fundamental sine component (neglecting harmonics) we get:

$$v_{as}^i = v_{DC} \frac{2}{\pi} \sin(\omega_0 t + \alpha + \theta) \quad (16)$$

$$v_{bs}^i = v_{DC} \frac{2}{\pi} \sin(\omega_0 t + \alpha + \theta - 2\pi/3) \quad (17)$$

$$v_{cs}^i = v_{DC} \frac{2}{\pi} \sin(\omega_0 t + \alpha + \theta + 2\pi/3) \quad (18)$$

$$i_{DC} = \frac{2}{\pi} \left(i_{as} \sin(\omega_0 t + \alpha + \theta) + i_{bs} \sin(\omega_0 t + \alpha + \theta - 2\pi/3) + i_{cs} \sin(\omega_0 t + \alpha + \theta + 2\pi/3) \right) \quad (19)$$

The approximation made above is valid if the harmonic component are small, compared to the fundamental. For a six pulse STATCOM harmonic components are substantial. However, if higher pulse numbers are used, harmonic can be reduced. The above analysis for a six pulse STATCOM can be easily extended to higher pulse number where the approximations are justified. From the point of view of control system design, we can neglect the harmonics though detailed simulation is necessary to validate any results. The STATCOM bus (phase) voltages are given by:

$$v_{as} = \sqrt{2/3} V_s \sin(\omega_0 t + \theta) \quad (20)$$

$$v_{bs} = \sqrt{2/3} V_s \sin(\omega_0 t + \theta - 2\pi/3) \quad (21)$$

$$v_{cs} = \sqrt{2/3} V_s \sin(\omega_0 t + \theta + 2\pi/3) \quad (22)$$

Where V_s is the line-to-line rms voltage and θ is the angle of the STATCOM bus. Note that α is the angle by which the fundamental component of converter output voltages leads the STATCOM bus voltages.

We use Kron's transformation to the D-Q-O variables:

$$\begin{bmatrix} i_{as} \\ i_{bs} \\ i_{cs} \end{bmatrix} = [C_k] \begin{bmatrix} i_{DS} \\ i_{QS} \\ i_{OS} \end{bmatrix} \quad (23)$$

Where:

$$[C_k] = \sqrt{\frac{2}{3}} \begin{bmatrix} \cos(\omega_0 t) & \sin(\omega_0 t) & 1/\sqrt{2} \\ \cos(\omega_0 t - 2\pi/3) & \sin(\omega_0 t - 2\pi/3) & 1/\sqrt{2} \\ \cos(\omega_0 t + 2\pi/3) & \sin(\omega_0 t + 2\pi/3) & 1/\sqrt{2} \end{bmatrix}$$

The ac phase voltages can also be transformed in a similar manner. Since $[C_k]^{-1} = [C_k]^T$ the transformation is power invariant, we can ignore i_0 because it is always zero. The equation in the D-Q variables are time invariant and are given by:

$$\begin{bmatrix} \frac{di_{DS}}{dt} \\ \frac{di_{QS}}{dt} \\ \frac{di_{DC}}{dt} \end{bmatrix} = [K_1] \begin{bmatrix} i_{DS} \\ i_{QS} \\ i_{OS} \end{bmatrix} + [K_2] \begin{bmatrix} v_{DC} \\ v_{QS} \end{bmatrix} \quad (25)$$

Where:

$$[K_1] = \begin{bmatrix} \frac{-R\omega_0}{x_l} & -\omega_0 & \frac{\omega_0 k \sin(\alpha + \theta)}{x_l} \\ \omega_0 & \frac{-R\omega_0}{x_l} & \frac{\omega_0 k \cos(\alpha + \theta)}{x_l} \\ \frac{\omega_0 k \sin(\alpha + \theta)}{b_c} & \frac{\omega_0 k \cos(\alpha + \theta)}{b_c} & \frac{-\omega_0}{b_c R_p} \end{bmatrix}$$



$$[K_2] = \begin{bmatrix} \frac{-\omega_0}{X_l} & 0 \\ 0 & \frac{-\omega_0}{X_l} \\ 0 & 0 \end{bmatrix}$$

$$\theta = \tan^{-1}(v_{DC}/v_{QS}) \text{ angle of STATCOM bus voltage,}$$

$v_s = \sqrt{(v_{Ds}^2 + v_{Qs}^2)}$ Magnitude of STATCOM bus voltage, α is the angle by which the converter output voltage leads the bus voltage, and $k = \sqrt{6}/\pi$

2.5 Decoupled Current Control System

The decoupled control system is based on a full d–q decoupled current control strategy using both direct and quadrature current components of the STATCOM ac current. The decoupled control system is implemented as shown in Fig. (6). A PLL which synchronizes on the positive sequence component of the three phase terminal voltage. The output of the PLL is the angle θ which used to measure the direct axis and quadrature axis component of the ac three phase voltages and current. The outer regulation loop comprising the ac voltage regulator which provide the reference current I_{qref} for the current regulator which is always in quadrature with the terminal voltage to control the reactive power. The voltage regulator is a proportional plus integral PI controller with K_p and K_i . The current regulator is also PI controller with K_p and K_i . The PLL system generates the basic synchronizing signal which is the phase angle of the transmission system voltage V_s , θ and the selected regulation-slope, k , determines the compensation behavior of the STATCOM device.

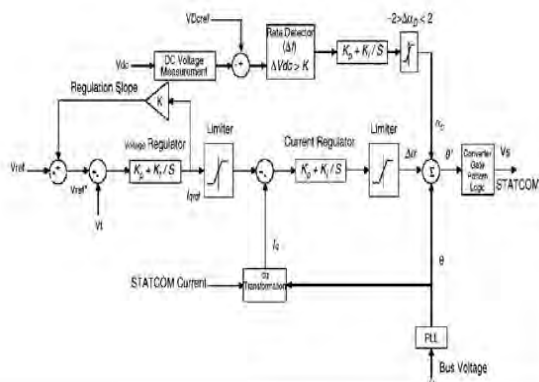


Figure 6. STATCOM d-q decoupled current control system

3. Studied System

The studied system is sample three-bus study system with the STATCOM located at bus B₂ consists of three phase ac source has supply one transmission line by 230kV and these transmission line supply three loads. The first load is inductive load (1+j0.8)pu, the second load is inductive load (0.7+j0.5)pu and the third load is change between two value the first value

capacitive load $(0.6-j0.4)$ pu and the second value is a dynamic load as shown in Fig. (7).

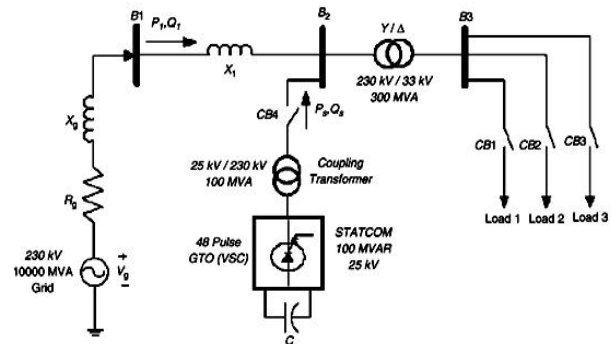


Figure 7 Three-bus study system with the STATCOM located at Bus B₂

The three-phase source implements a balanced three-phase voltage source with internal R-L impedance, and a transmission line parameter model with lumped losses are based on the Bergeron's traveling wave method used by the Electromagnetic Transient Program (EMTP). The lossless distributed LC line is characterized by two values, the surge impedance $Z_c = \sqrt{LC}$ and the phase velocity $v = 1/\sqrt{LC}$. The model uses the fact that the quantity $e + Zi$ (where e is line voltage and i is line current) entering one end of the line must arrive unchanged at the other end after a transport delay of $\tau = d/v$, where d is the line length. By lumping $R/4$ at both ends of the line and $R/2$ in the middle and using the current injection method, the following two-port model is derived as shown in Fig. (8).

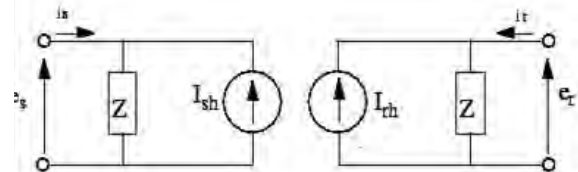


Figure 8. Multiphase line models

Where:

$$Z = Z_C(R/4)$$

$$h = \frac{Z_C - (R/4)}{Z_C + (R/4)}$$

$$Z_C = \sqrt{L/C}$$

$$\tau = d\sqrt{LC}$$

The three-phase transformer implements a three-phase transformer using three single-phase transformers. The loads implement a three-phase balanced load as a parallel combination of RLC elements. At the specified frequency, the load exhibits constant impedance. The three-phase dynamic load implements a three-phase, three-wire dynamic load whose active power P and reactive power Q vary as function of positive-sequence



voltage. The load impedance is kept constant if the terminal voltage V of the load is lower than a specified value V_{min} . When the terminal voltage is greater than the V_{min} value, the active power P and reactive power Q of the load vary as follows:

$$P(s) = P_o \left(\frac{V}{V_o} \right)^{n_p} \frac{(1 + T_{p1}s)}{(1 + T_{p2}s)} \quad (26)$$

$$Q(s) = Q_o \left(\frac{V}{V_o} \right)^{n_q} \frac{(1 + T_{q1}s)}{(1 + T_{q2}s)} \quad (27)$$

Where: V_o is the initial positive sequence voltage, P_o and Q_o are the initial active and reactive powers at the initial voltage, V is the positive-sequence voltage, n_p and n_q are exponents (usually between 1 and 3) controlling the nature of the load, T_{p1} and T_{p2} are time constants controlling the dynamics of the active power P , and T_{q1} and T_{q2} are time constants controlling the dynamics of the reactive power Q . Table 1. Shows the three bus studied system with STATCOM parameters. Also, phase shift pattern for 24-pulse converter as in Appendix.

Table 1. Three bus studied system consist of two loads with STATCOM parameters.

Three phase ac source Rated Voltage: 1.03p.u Short circuit capacity:10000 MVA Frequency: 60Hz Base Voltage: 230 kV X/R ratio: 8	STATCOM: Primary Voltage: 138 kV Secondary Voltage: 15 kV Nominal Power: 100 MVAR. Frequency: 60 Hz Equivalent Capacitance: 750μF
Power Transformer(Y/Δ): Primary Voltage: 230 kV. Secondary Voltage: 33 kV. Frequency: 60 Hz Rated Power: 300 MVA Magnetization Resistance: 500 p.u., and Magnetization Reactive: 500 p.u	Coupling Transformer (Y/Y): Primary Voltage: 138 kV Secondary Voltage: 230 kV Frequency: 60 Hz Rated Power: 100 MVA
Capacitor Bank (dc): Total Capacitance: 10 mf Rated dc Voltage: 2 kV	Transmission Line Resistance: R_{TL} : 0.05 p.u. Reactance: X_{TL} : 0.2 p.u.
GTO Switches Snubber Resistance: 100 KΩ Snubber capacitance: inf. Internal Resistance: 0.1 Ω No. of Bridge arm: 3	Three Phase Loads Load 1 (1.0+j0.8) p.u Load 2 (0.7+j0.5) p.u Load 3 (0.6-j0.4) p.u

4. Results and Discussions

The system study radial power system is subjected to load switching at Bus B_3 . At starting, the source voltage is such that the STATCOM is active. It does absorb provide reactive power to the network. The capacitor bank is recharged to 1.0 pu voltage. The network voltage, V_g , is 1.02 pu and only capacitive load 3 with ($P=0.6pu$ and $Q=0.4 pu$) (at rated voltage) is connected at load Bus B_3 and the STATCOM B_2 bus voltage is 1.2pu for the uncompensated system and the transmitted real and reactive power are $P_L=0.7pu$ and $Q_L=-0.4pu$.

The results can summarize for the four cases are the following load excursion sequence:

Case 1: At $t=0.5 sec$ load 1 inductive load ($1+j0.8 pu$) injection, at $t=1 sec$ load 2 inductive load ($0.7+j0.5$) injection, at $t=1.5 sec$ load 3 capacitive load and load 2 inductive load rejection.

Case 2: At $t=0.5sec$ load 2 inductive load ($0.7+j0.5pu$) injection, at $t=1 sec$ load 1 inductive load ($1+j0.8$) injection, at $t=1.5 sec$ load 3 capacitive load and load 2 inductive load rejection.

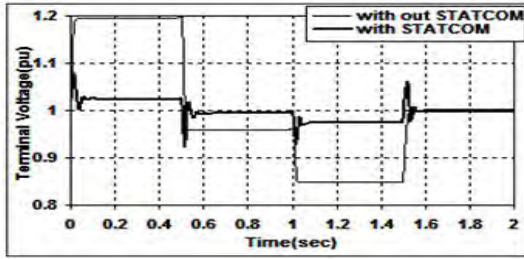
Case 3: At $t=0.5 sec$ load 1 inductive load ($1+j0.8 pu$) injection, at $t=1 sec$ load 2 inductive load ($0.7+j0.5$) injection, at $t=1.5 sec$ load 3 Dynamic load and load 2 inductive load rejection.

Case 4: At $t=0.5sec$ load 2 inductive load ($0.7+j0.5pu$) injection, at $t=1 sec$ load 1 inductive load ($1+j0.8$) injection, at $t=1.5 sec$ load 3 Dynamic load and load 2 inductive load rejection.

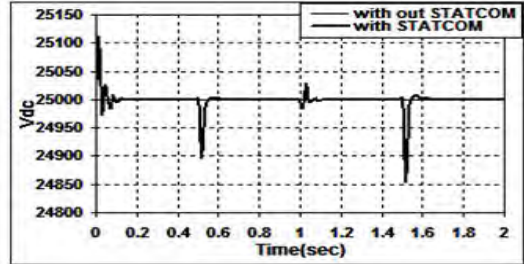
Figure 9(a) is represented case 1, the terminal voltage without STATCOM is 1.2 pu starting and 0.96 pu after injection inductive load 1, 0.85 pu after injection inductive load 2, and near unity after rejection of both capacitive (load 3) and inductive (load 2) loads. Whenever, with STATCOM the sequences of the terminal voltage are 1.02 pu, 1.0 pu, 0.97 pu, and unity respectively. The active and reactive powers of the transmission line are starting (0.9-j1.3) pu without STATCOM and are (1.43+j0.1) pu after inductive load 1 injected, (1.5+j0.75) pu after inductive load 2 injected, and back to (0.7-j0.25) pu after rejected of both inductive load 2 and capacitive load. While with STATCOM the sequence of the active and reactive powers on the transmission line are: (0.7-j0.4) pu, (1.5-j0.1) pu, (2.0+j0.2) pu, and (0.7-j0.25) pu respectively as shown in Fig. 9(e, f). A DC voltage and currents of the VSC STATCOM as shown in Fig. 9(b, c, d) respectively.

From Fig. (10) to Fig. (12) are depicts the terminal voltage, DC voltage, voltage and current of the VSC STATCOM, active and reactive power in the transmission line, all these in three cases (case 2 to case 4) respectively. Figure (13) discusses the reactive power supply and absorbing by STATCOM for different cases. When load 1, and load 2 are inductive load and load 3 is capacitive load as shown in Fig. 10(a). While the reactive power supply and absorbing by STATCOM when load 1, and 2 are inductive load and load 3 is Dynamic load as shown in Fig. 13(b). STATCOM absorbed reactive power 0.8pu at starting and delivered 0.2 pu after injection inductive load 1, 0.82pu after injection inductive load 2, and near zero after rejection of both capacitive load 3 and inductive load 2 loads.

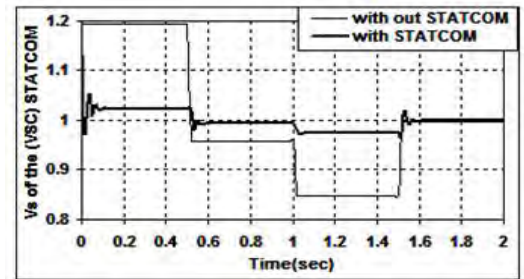




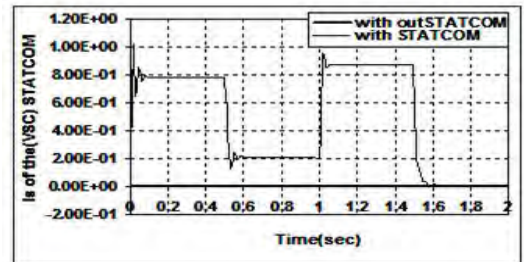
(a) Terminal voltage at bus 2



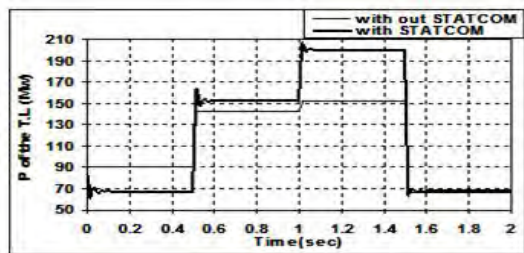
(b) DC voltage of the STATCOM



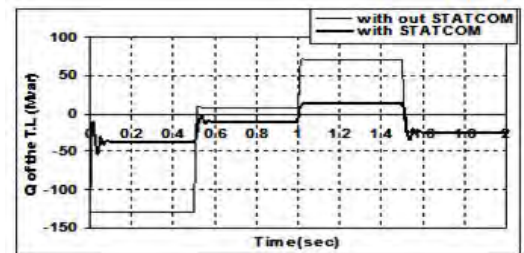
(c) Voltage of the (VSC) STATCOM.



(d) Current of the (VSC) STATCOM.

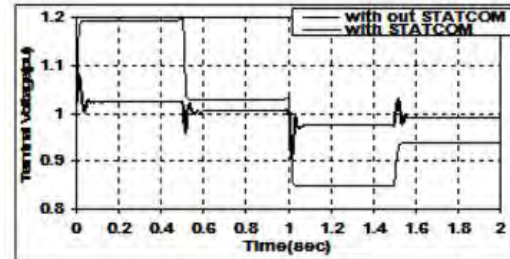


(e) Active power in the transmission line.

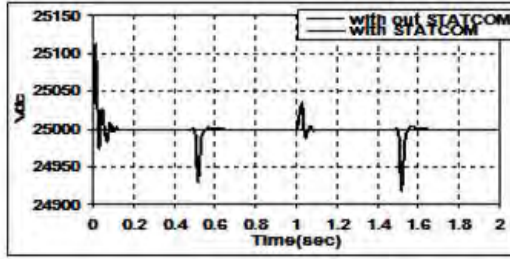


(f) Reactive power in the transmission line.

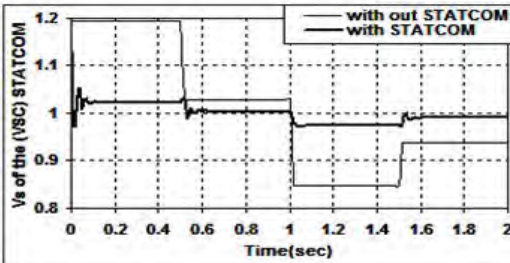
Figure 9. Transient performance of the STATCOM when load 1, 2 inductive load and load 3 capacitive load (case 1).



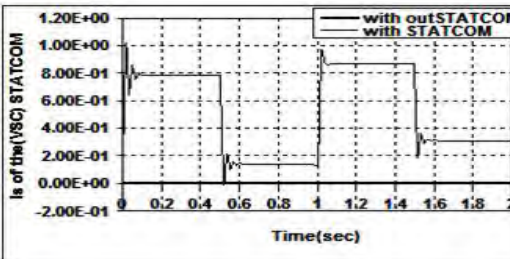
(a) Terminal voltage at bus 2



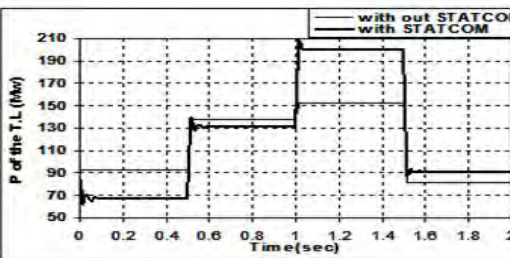
(b) DC voltage of the STATCOM



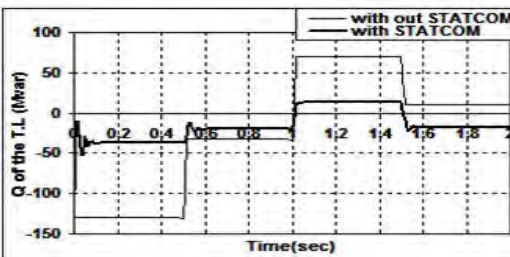
(c) Voltage of the (VSC) STATCOM.



(d) Current of the (VSC) STATCOM.



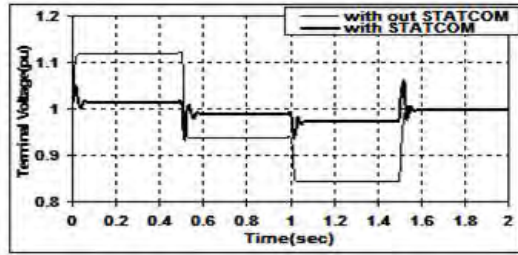
(e) Active power in the transmission line.



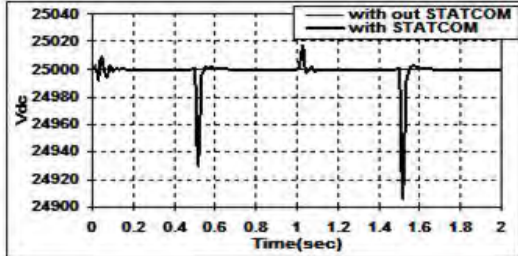
(f) Reactive power in the transmission line.

Figure 10. Transient performance of the STATCOM when load 1, 2 inductive load and load 3 capacitive load (case 2).

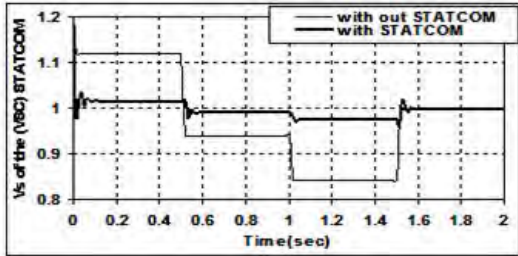




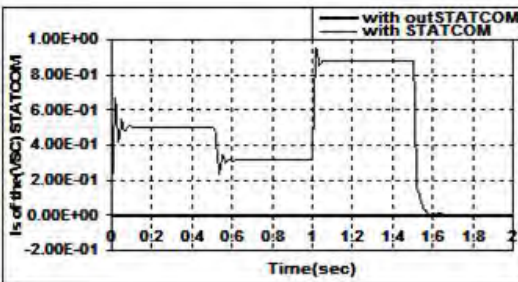
(a) Terminal voltage at bus 2



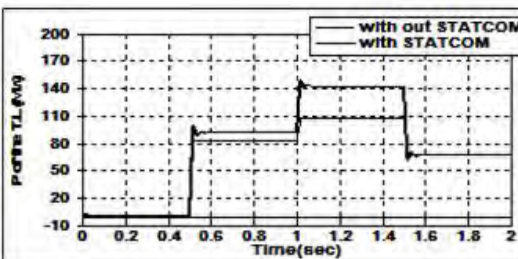
(b) DC voltage of the STATCOM



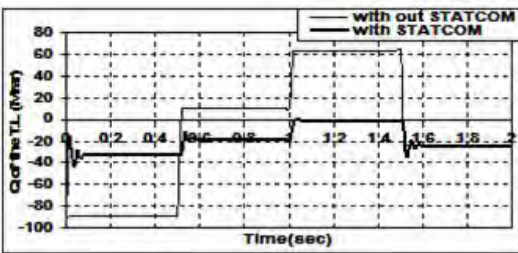
(c) Voltage of the (VSC) STATCOM.



(d) Current of the (VSC) STATCOM.

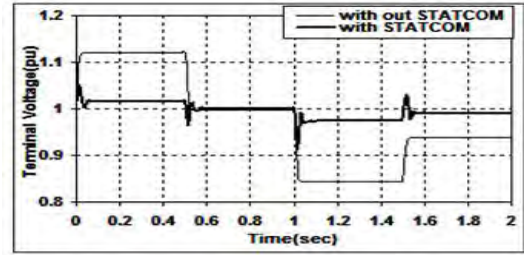


(e) Active power in the transmission line.

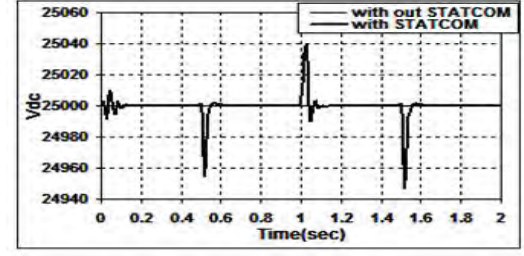


(f) Reactive power in the transmission line.

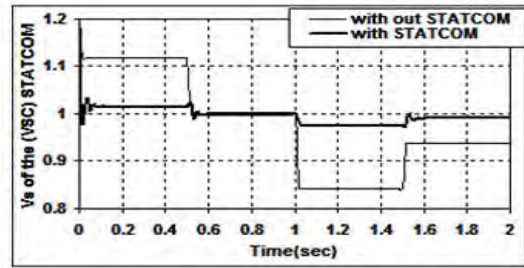
Figure 11. Transient performance of the STATCOM when load 1, 2 inductive load and load 3 Dynamic load (case 3).



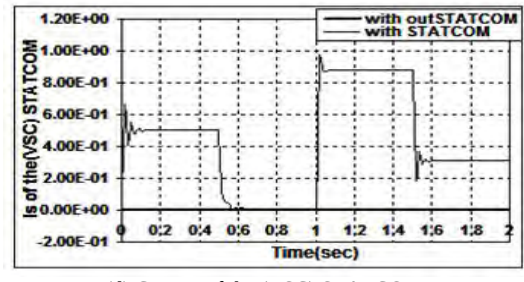
(a) Terminal voltage at bus 2



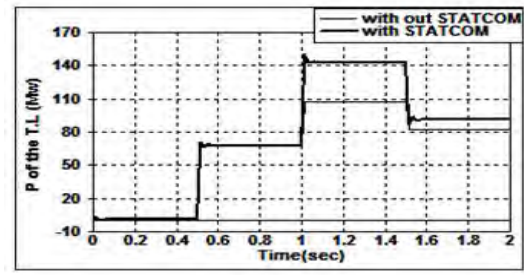
(b) DC voltage of the STATCOM



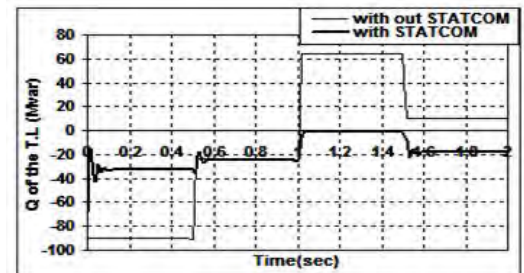
(c) Voltage of the (VSC) STATCOM.



(d) Current of the (VSC) STATCOM.



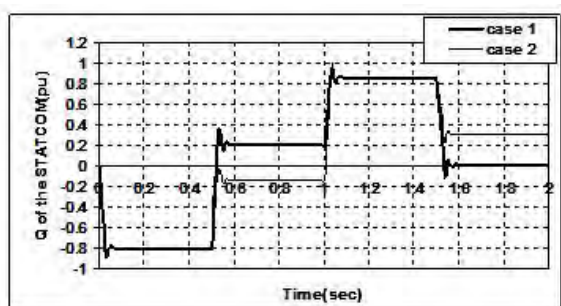
(e) Active power in the transmission line.



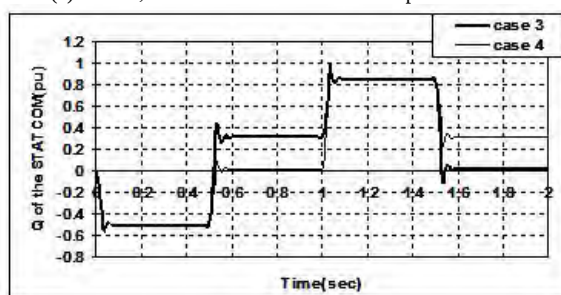
(f) Reactive power in the transmission line.

Figure 12. Transient performance of the STATCOM when load 1, 2 inductive load and load 3 Dynamic load (case 4).





(a) Load 1, 2 inductive load and load 3 capacitive load



(b) Load 1, 2 inductive load and load 3 Dynamic load

Figure 13. Reactive power supply and absorbing by STATCOM for different cases

5. Conclusions

Numerical simulations are reported for a STATCOM based on the proposed converter, showing simplicity both in control and converter topology, as well as effective tracking performance of the sliding controller. GTO voltage source converter of STATCOM device model is presented. These full descriptive digital models are validated for voltage stabilization reactive compensation in a sample three-bus study system. The performance of the STATCOM when injection and rejection loads. The maximum load ability and voltage profiles of power system can be significantly improved by STATCOM. When hitting the current limit, the related system buses will lose the controllability and bus voltages decreased rapidly along with the loading level.

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Appendix

A phase shift pattern for 24-pulse converter are:

PST +7.5° Necessary to eliminate the 24-pulse harmonics
+3.75° Necessary to eliminate the 48-pulse harmonics

Total +11.25° Winding turn rate 1: tan (11.25°)

Driver -7.5° Necessary to eliminate the 24-pulse harmonics
-3.75° Necessary to eliminate the 48-pulse harmonics

Total -11.25°

The resultant output voltage generated by the first 12- pulse converter is:

$$v_{ab12}(t)_1 = 2[V_{ab1} \sin(wt + 30^\circ) + V_{ab11} \sin(11wt + 195^\circ) + V_{ab13} \sin(13wt + 255^\circ) + \dots + V_{ab23} \sin(23wt + 60^\circ) + V_{ab25} \sin(25wt + 120^\circ) + \dots]$$

PST -7.5° Necessary to eliminate the 24-pulse harmonics
+3.75° Necessary to eliminate the 48-pulse harmonic

Total -3.75° Winding turn rate 1: tan (3.75°)
Driver +7.5° Necessary to eliminate the 24-pulse harmonics
-3.75° Necessary to eliminate the 48-pulse harmonics

Total +3.75°

The resultant output voltage generated by the second 12-pulse converter is:

$$v_{ab12}(t)_2 = 2[V_{ab1} \sin(wt + 30^\circ) + V_{ab11} \sin(11wt + 15^\circ) + V_{ab13} \sin(13wt + 75^\circ) + \dots + V_{ab23} \sin(23wt + 60^\circ) + V_{ab25} \sin(25wt + 120^\circ) + \dots]$$

Biographies



Youssef A. Mobarak was born in Egypt. He received his B.Sc. and M.Sc. degrees in Electrical Engineering from South Valley University, Aswan, Egypt, in 1997 and 2001 respectively and Ph.D. from Cairo University, Egypt, in 2005. He joined Electrical Engineering Department, High Institute of Energy, South Valley University as a Demonstrator, as an Assistant Lecturer, and as an Assistant Professor during the periods of 1998–2001, 2001–2005, and 2005–2009 respectively. He joined Artificial Complex Systems, Hiroshima University, Japan as a Researcher 2007–2008. Also, he joined King Abdul-Aziz University, Rabigh, Faculty of Engineering 2010 to present. His research interests are power system planning, operation, and optimization techniques applied to power systems, Nanotechnology materials via addition nano-scale particles and additives for usage in industrial field.





Nouvelle approche de diagnostic des états redoutés dans un système dynamique hybride

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Résumé

Un système dynamique hybride est décrit par un ensemble de variables continues et un ensemble d'événements discrets interagissant mutuellement. La réalité impose en outre de prendre en compte les défaillances des composants ou les incertitudes sur la connaissance du système. L'accroissement incessant de la complexité des systèmes automatiques a fait en sorte que la tâche de la surveillance devienne de plus en plus moins aisée. De ce fait, les systèmes peuvent fonctionner en plusieurs modes. Certains de ces modes correspondent à un fonctionnement normal et d'autres représentent des modes défaillants. Dans cet article, nous nous intéressons à l'évaluation de la probabilité d'occurrence des événements défaillants dans un système dynamique hybride. Nous proposons pour cela, une méthode d'évaluation basée sur la connaissance a priori du système, au moins du point de vue de l'état de ses composants en fonctionnement normal (fermé ou ouvert, éteint ou allumé,... etc) Afin de valider notre proposition, nous l'avons appliquée au benchmark utilisé par différents auteurs pour le calcul de la fiabilité dynamique [1]. Notre objectif est donc d'évaluer la probabilité d'occurrence des deux événements redoutés du cas test : l'assèchement et le débordement d'un réservoir. Cet exemple a pour principal intérêt de mettre en lumière les problématiques de la sûreté de fonctionnement des systèmes.

Abstract

A dynamic hybrid system is described by a set of continuous variables and a set of discrete events interacting mutually. The reality imposes to take into account the failures of constituents (components) or the uncertainties on the knowledge of the system. The ceaseless increase of the complexity of the automatic systems made so that the task of the surveillance becomes more and more less easy. Therefore, the systems can work in several modes. Some of these modes correspond to a normal functioning and the

others represent failing modes. In this article, we are interested in the evaluation of the probability of occurrence of the failing events in a hybrid dynamic system. We propose for it, a valuation method based on the knowledge in priori of the system, at least from the point of view of the state of its components in normal functioning (closed or opened, put out or lit, etc.). To validate our proposition, we applied her to the benchmark used by various authors for the calculation of the dynamic reliability. Our objective thus is to estimate the probability of occurrence of both events dreaded by the test case: the drying out and the overflowing of a reservoir.

This example has for main interest to bring to light the problems of the safety of functioning of the systems.

Mots-Clés : *Diagnostic, systèmes dynamiques hybrides, surveillance, sûreté de fonctionnement.*

Keywords: *Diagnosis, dynamic hybrid systems. Reliability*

1. Introduction

NOUS représentons un système, d'un point de vue diagnostic comme étant un cycle de vie qui est interrompu par l'occurrence d'un événement qui fait basculer le mode de fonctionnement du système d'un état normal à un état de fonctionnement défaillant. En adoptant cette représentation, le concepteur peut aisément remarquer que son apport ne peut se manifester qu'au niveau du bloc détection pour ensuite reconfigurer le système. L'occurrence de l'événement, on ne peut plus aléatoire. L'objectif de cette étude est précisément de dénombrer et caractériser les états défaillants ou ce que nous appelons états redoutés et d'évaluer la probabilité d'occurrence des événements qui mènent vers l'état défaillant.

Un système est un ensemble de composants et le composant, dans notre cas, est considéré comme une



entité représenté par un nombre binaire (bit). Le fonctionnement du système est divisé en étapes temporelles. Chaque étape est considérée comme telle avec tous ses paramètres. On considère une étape de fonctionnement comme un sous-état de fonctionnement qui peut être un état normal comme il peut être un état défaillant. Dans le cas le plus simple, le système peut être considéré comme une seule étape. Les paramètres de l'état sont les valeurs des bits représentant les composants du système (1 ou 0 par exemple fermé, ouvert ; éteint, allumé ...), on n'entend pas ici défaillant ou non. Le sous-système avec l'étape temporelle correspondante en question est alors une combinaison des bits représentatifs des composants. Une étape faisant intervenir N composants aura 2^N combinaisons représentant 2^N sous-états possibles (défaillants et normaux). Aussi, chaque combinaison nous informe de l'état de chaque composant et indirectement, elle nous informe aussi sur les composants qui sont à l'origine de l'état défaillant. Une fois les sous-états sont simulés, les résultats sont analysés et les modes défaillants sont repérés. Après quoi, un calcul de probabilité est effectué.

- ✚ Décomposer le fonctionnement total du système en étapes de fonctionnement où les composants intervenant ont un seul état.
- ✚ Repérer les composants intervenant dans chaque étape
- ✚ Représenter chaque composant par un bit $B \{1,0\}$
1 : Etat demandé et 0 : Etat contraire
- ✚ Représenter l'étape par un nombre binaire dont le nombre de bits est égal au nombre de composants intervenant dans l'étape en question
- ✚ Exprimer les combinaisons représentatives de l'étape (sous-états de fonctionnement relatif à l'étape)
- ✚ Analyser les résultats
- ✚ Calculer la probabilité d'occurrence de chaque état défaillant

2. Le cas test

La représentation d'un système dynamique d'un point de vue diagnostic est schématisée sur la figure.1. Il est considéré comme étant un cycle de vie qui est interrompu par l'occurrence d'un événement qui fait basculer le mode de fonctionnement du système d'un état normal à un état de fonctionnement défaillant. Le cas test consiste en un réservoir contenant un liquide dont le niveau h doit être maintenu à l'aide d'une pompe principale P1, d'une pompe de secours et d'une vanne de vidange V.

Chacun des trois composants est commandé par une boucle de contrôle contenant un détecteur de niveau (figure 2).

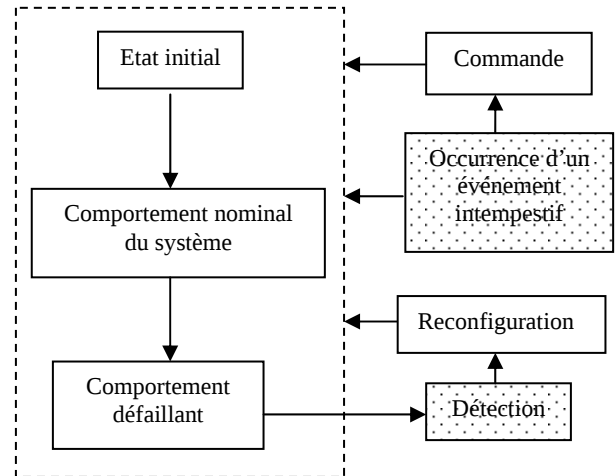


Figure.1. Système de régulation de niveau

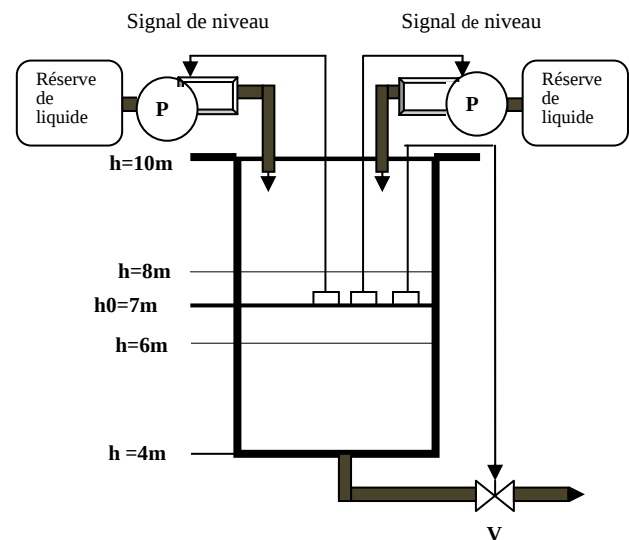


Figure.2. Etat global d'un système dynamique

La mission à remplir est de maintenir le niveau du liquide constant dans un intervalle $[6,8]$. Si le niveau est inférieur à 6, la commande ferme la vanne et ouvre la pompe de secours et si le niveau est supérieur à 8, les pompes sont arrêtées et la vanne est ouverte. Deux situations extrêmes peuvent se produire : l'assèchement et le débordement. Ces deux cas arrivent quand la commande ne peut plus agir sur les composants du système qui deviennent alors défaillants (1 ou plusieurs composants). On dit alors que le système est dans un état défaillant voir dangereux. Les trois composants la pompe P1, la pompe P2 et la vanne V sont mutuellement indépendants et non réparables.

La variable continue pour le système est le niveau du liquide h lequel est fonction de l'état des composants. Ainsi, l'équation différentielle pour le système est donnée par :



$$\frac{dh(t)}{dt} = \gamma(v) \quad (1)$$

où $v = (v_{P1}, v_{P2}, v_V)$

$$\text{Avec } v_c = \begin{cases} 0 & \text{si } c \text{ est OFF} \\ 1 & \text{si } c \text{ est ON} \end{cases} \quad (2)$$

avec $\gamma(v) = (v_{P1} + v_{P2} - v_V)D$

où D est le débit des éléments P1, P2 et vanne.

L'équation généralisée (1) reflète les différents modes opératoires possibles du processus. Elle fait apparaître l'influence des phénomènes discrets sur l'évolution du processus au travers des termes v_{P1}, v_{P2}, v_V . Ces derniers peuvent prendre, dans le cas de cet exemple, la valeur 1 si le composant est actif ou bien si une défaillance le bloque dans la position ouverte ou active, et la valeur 0 dans le cas contraire, comme l'exprime l'équation (2). En conditions nominales, le débit de P1 est égal au débit de P2 et de V. Ainsi $D = 1,5 \text{ m}^3\text{h}^{-1}$ pour P1, P2 et V.

Au temps $t = 0$, le niveau du liquide $h = 7 \text{ m}$, la pompe P1 fonctionne, P2 est à l'arrêt et la vanne V est ouverte.

Les lois de commande du système qui définissent l'état des pompes et de la vanne en fonction du niveau de liquide sont données dans le tableau 1 suivant :

Tableau 1: Les lois de commande du système

Niveau $h(\text{m})$	Pompe P1	Pompe P2	Pompe P3
$h < 6$	Ouverte	Ouverte	Fermée
$6 < h < 8$	Ouverte	Fermée	Ouverte
$h > 8$	Fermée	Fermée	Ouverte

Le système ainsi défini peut être considéré comme un automate hybride qui prend en compte les différents modes continus de fonctionnement du système et le passage de l'un à l'autre sur l'occurrence des événements déterministes qui sont produits par franchissement de seuils des variables continues. Les automates élémentaires sont donnés sur la figure 3.

Les événements associés à P1, P2, P3 sont:

- a_{P1} et a_{P2} : arrêt des pompes P1 et P2
- d_{P1} et d_{P2} : démarrage des pompes P1 et P2
- o_V : ouverture de la vanne et
- f_V : fermeture de la vanne
-

Nous distinguons pour les composants P1, P2 et V les états suivants :

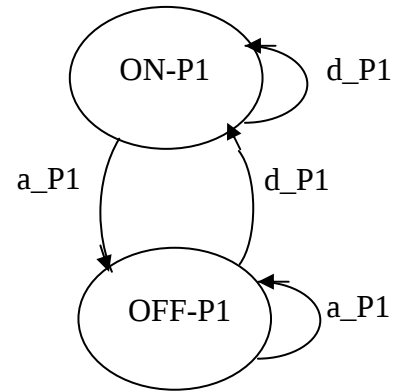
- ON-P1, ON-P2 et ON-V : pompes actives et vanne ouverte,
- OFF-P1, OFF-P2 et OFF-V : pompes en arrêt et vanne fermée,

Pour le réservoir nous avons les états ;

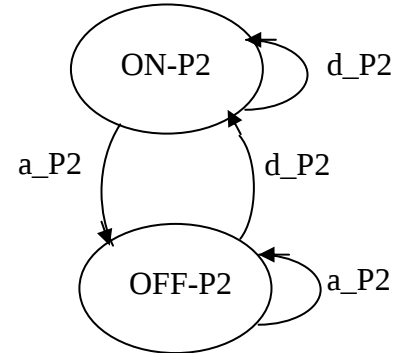
- N_n : niveau normal du réservoir ($6 \leq h \leq 8$)
- N_{ass} : niveau d'assèchement ($h \leq 4$) et
- N_{deb} : niveau de débordement ($h \geq 10$).

L'automate représentant la commande est donné sur la figure 4. Les lois de contrôle de la commande:

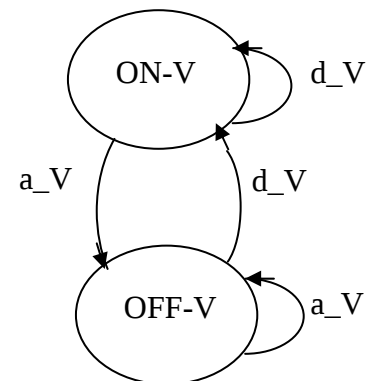
- initial : C0 – P1 active, P2 arrêtée et V ouverte,
- si $h < 6$: C2 → C3, P1 active ; C3 → C4, P2 active et C4 → C5, V fermée,
- si $h > 8$: C6 → C7, P1 arrêtée ; C7 → C8, P2 arrêtée et C8 → C1, V ouverte,



(a) Automate de la pompe P1

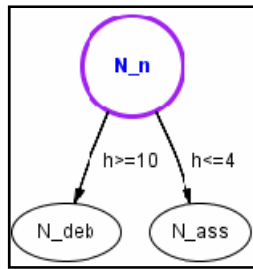


(b) Automate de la pompe P2



(c) Automate de la vanne





(d) Automate du réservoir

Figure.3. Automates à états finis des trois composants

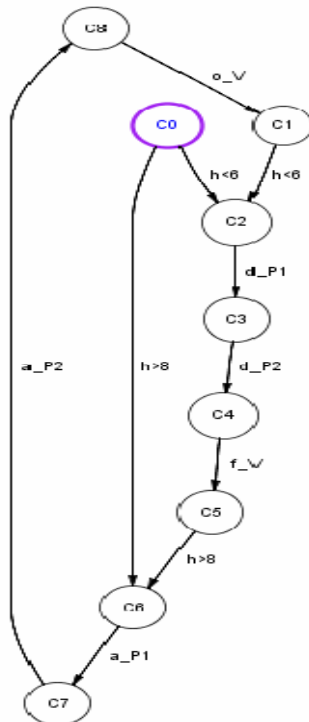


Figure.4. Automate de la commande

3. Application de la méthode

Détection des états défaillants

Le fonctionnement total du système peut-être décrit par une seule étape ou bien un seul état à savoir maintenir le niveau du liquide h dans l'intervalle $[6,8]$. La simulation du système sans occurrence d'événements intempestifs donne la courbe suivante sachant que le niveau initial est $h=7m$. Le système vu comme ça, maintient le niveau du liquide constant (figure 5).

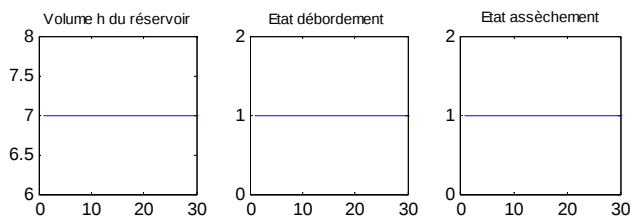
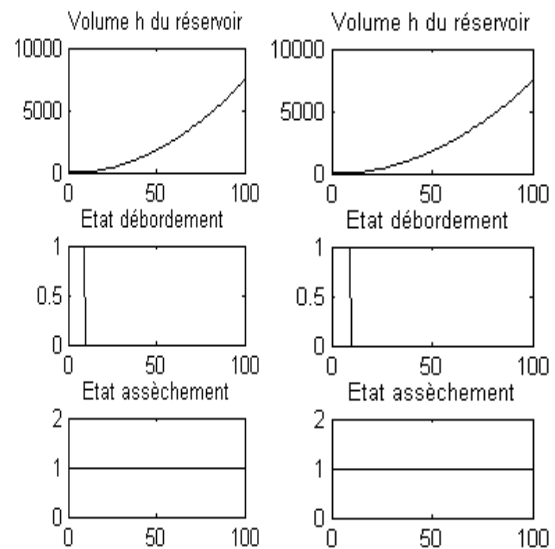


Figure.5. Fonctionnement nominal du système

La simulation d'une panne à la 9^{ème} itération (vanne bloquée en fermeture) le niveau du liquide augmente indéfiniment : débordement et l'indicateur de cet état défaillant se met à 0 (figure 6). La simulation d'une autre panne (la pompe P1 n'arrive pas à s'ouvrir et la pompe P2 est bloquée en fermeture, la vanne étant ouverte), le niveau descend au plus bas niveau et l'indicateur de l'état assèchement est passé à 0. Il faut noter que les indicateurs des deux états assèchement et débordement ont été initialisés à 1.



a) Assèchement

b) Débordement

Figure.6. Etats défaillants du système

Calcul de Probabilité

La figure 7 représente les différentes combinaisons possibles (sous-états). Le système démarre avec l'état initial normal (101 : Pompe P1 ouverte, Pompe P2 fermée et la vanne V ouverte). A $t=9$, il ya l'occurrence de l'état défaillant (un, deux ou les trois composants ne sont pas dans l'état attendu). Pour éviter toute ambiguïté avec le rôle de la commande, celle-ci est inhibée à l'occurrence de l'événement redouté. Par ailleurs, on ne s'intéresse pas ici à la cause physique de la défaillance. Celle-ci peut être due, en effet, à une action externe (fermeture ou ouverture intempestive), au blocage du composant dans son état (ouverture ou fermeture) ou tout simplement cette défaillance est la conséquence de la durée de vie du composant (usure). A partir de l'état normal, on s'intéresse alors à toutes les possibilités de défaillances. Ces modes de fonctionnement sont résumés dans ce qui suit, à noter que seul l'état 101 est l'état de fonctionnement nominal.

1/ **000**: La pompe P1 est fermée, la pompe P2 est fermée et la vanne est fermée. **Etat stationnaire dans l'état initial.**



2/ **001** La pompe P1 fermée, la pompe P2 fermée et la vanne ouverte **Assèchement**

3/ **010** la pompe P1 est fermée, la pompe P2 est ouverte et la vanne est fermée **Débordement**

4/ **011** la pompe P1 est fermée et la pompe P2 est ouverte, la vanne est ouverte. La pompe de secours prend le relais de la pompe P1. **Etat normal (de secours)**

5/ **100** la pompe P1 est ouverte la pompe P2 est fermé et la vanne fermée **Débordement**

7/ **110** la pompe P1 ouverte la pompe P2 ouverte et la vanne fermée **Débordement**

8/ **111** la pompe P1 ouverte la pompe P2 ouverte et la vanne ouverte **Débordement**

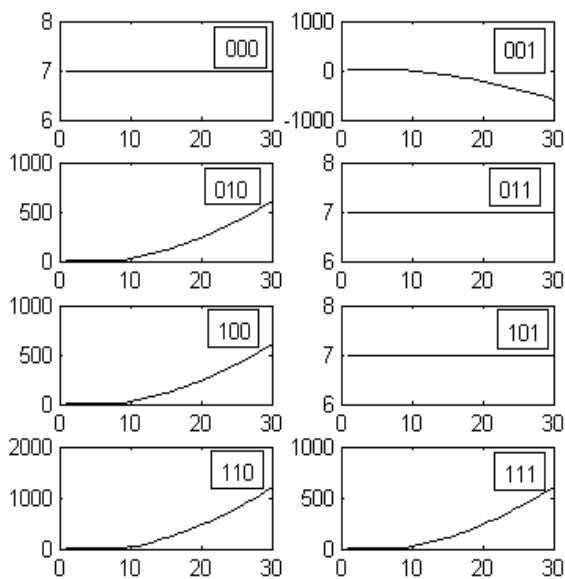


Figure.7. Niveau du liquide h pour tous les modes

Les résultats de la simulation ont montré que tous les modes défaillants ont été détectés. Ainsi, le mode débordement apparaît dans 4 cas, le mode assèchement 1 fois et l'état normal 3 fois (Tableau 2).

Etat normal	Assèchement	Débordement
000	011	010
011		100
101		110
		111

Tableau 2 : Tableau récapitulatif des états

Le mode correspondant à la combinaison 000 où tous les composants sont fermés, le niveau du liquide ne varie pas de l'état initial. Pour le mode (011), la pompe P1 est fermée et la pompe P2 prend le relais, cet état correspond à l'état normal et le niveau du liquide est constant.

Le calcul de probabilité d'occurrence des modes défaillants donne explicitement les valeurs (Tableau 3)

Etat	Normal	Assèchement	Débordement
Probabilité	0,375	0,125	0,5

Tableau 3 : Valeurs de probabilité de chaque état

Les résultats obtenus montrent que la probabilité de l'état assèchement est de 0,125 et la probabilité d'occurrence de l'état débordement est de 0,5. Si nous observons les valeurs de probabilité obtenues dans les études relatives à la fiabilité dynamique à $t=1000$ Heures [1][2], les résultats sont très proches et si on augmentait le temps à plus de 1000h, les valeurs d'occurrence des deux événements s'approcheraient de plus en plus des valeurs que nous avons trouvées jusqu'à se stabiliser à celles-ci.

Tableau 4 : Probabilités d'accès aux états redoutés

Temps(h)	Débordement			Assèchement		
	PMDPM	RdP	ASH	PMDPM	RdP	ASH
1000	0,486	0,486	0,475	0,118	0,118	0,117

4. Conclusion

Dans ce travail, nous avons proposé une stratégie pour le diagnostic des systèmes dynamiques hybrides. Nous avons, ainsi, introduit une approche conduisant à fournir une aide pour l'identification et la localisation des modes défaillants ainsi que le calcul de probabilité de leurs occurrences. Notre approche nous a permis d'évaluer la probabilité d'occurrence des événements redoutés du cas test. Les résultats trouvés sont très probants. La simulation a été faite sur matlab. La prochaine étape est d'appliquer cette méthode sur des exemples encore plus complexes.

5.References

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Strategy for Analysis and Design of Digital Robust Control Systems

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Abstract

This paper suggests a strategy for analysis and design of a digital robust control system, capable of suppressing the effects of the variations of its parameters. The methodology used for the robust controller design is compensation with two degrees of freedom that brings as a result a desired system's performance and stability. The control system is analyzed in terms of stability with the aid of the D-partitioning method before and after being compensated. The system's performance is also examined for robustness in the time domain. The analysis shows a considerably improved performance in terms of system's robustness. The suggested robust controller build by two microcontrollers can be applied to any complex digital control system with variable parameters.

Keywords: Variable parameters, D-partitioning, Discrete-time, Robust controller, Microcontrollers;

1. Introduction

The objective of the paper is to propose a strategy for analysis and design of an optimal robust controller that can be applied to digital control systems with variable parameters. It is common knowledge that the problem of digital compensators of the type of PI or PID controllers using difference equations is already resolved in literature, whereas this research is focusing mainly on the challenges of the digital robust control design. The technique employed for the robust controller design is achieved by applying series compensation with a unity feedback, combined with a forward compensation. The controller, operating in two degrees of freedom, enforces a desired system damping, margin of stability and time response. The designed robust controller suppresses the effects of parameter variations so that any parameter uncertainties would affect insignificantly the final system performance. The robust analysis and control of the digital system are discussed in the discrete-time domain. The lack of a comprehensive, simple and user friendly analysis tool for digital control systems and further the absence of a robust controller that can make a digital system insensitive for any parameter variation within specific limits are the motivating factors for this research. Following some initial ideas of Neimark's D-partitioning

[1, 2], the method is further advanced in this research in terms of its application for digital control systems. The employment of the D-partitioning enables a quick and convenient determination of the regions of stability in case of variation of system's parameters and its effect on the system's stability. The D-partitioning method is applied for analysis of digital system's stability before and after implementing the designed robust controller. The employment of MATLAB enables automatic plotting and analysis of the regions of stability. Further analysis in terms of the system's time response in the discrete-time domain is showing the efficiency of the designed digital robust controller.

In this research, a simulation model, representing a complex real-time plant of high order is preliminary reduced and approximated to a third order system following a well known approximation criterion [3]. Although the complexity of the examined simulation model is significantly lower than the real-world plant, the achieved results can be implemented to any advanced system. In reality, most control systems contain elements that drift or vary with time and the initial correct determination of the exact parameter values is often quite challenging and problematic [3, 4].

The shortcomings of a number of known optimization methods are the requirements of very accurate models of the systems. Optimizing systems with wrongly determined models often makes their performance worse rather than better. Alternatively, the suggested in this research analysis and design methods are to a certain extent insensitive to parameter drift. The applied compensation technique with two degrees of freedom, used for the design of the digital robust controller, even allows its operation in cases of simultaneous variation of a number of uncertain parameters, bringing the system to the desirable performance.

2. The Concept of Digital Robust Control Systems

A usually known block diagram of digital control system, shown in Figure 1, illustrates the interaction between its components. The interface at the input of the digital



computer is an analogue-to-digital converter (ADC). It converts the continuous-time error signal $E(s)$ into a digital binary format signal that can be processed by the computer. At the computer output a digital-to-analogue converter (DAC) is required to convert the computer binary signals into continuous-time signal format that is necessary to drive the plant. A clock synchronizes the operation of the ADC, digital computer and DAC. The digital computer in combination with its interface, ADC and DAC, operates as a digital compensator within the system. A sensor, included in the negative feedback of the system, is representing a transducer that converts the plant's variable under control into a continuous signal of a desired format [5, 6]. In the following discussions on the system performance, to simplify the analysis, it is

assumed that the sensor's transfer function has a unity value, implying a system's unity feedback.

Since the digital computer can be programmed to perform any form of numerical computation, the digital compensator can realize any type of controller equation or algorithm known as the difference equation. Further, the digital control system shown in Figure 1 is modified, taking into account that the combination of the ADC, digital computer and DAC can be accurately represented by the combination of an ideal sampler, with a sampling period T , a digital filter $D(z)$, that solves the difference equation, and a zero-order hold (ZOH) [5, 6]. The complete modified model is shown in Figure 2.

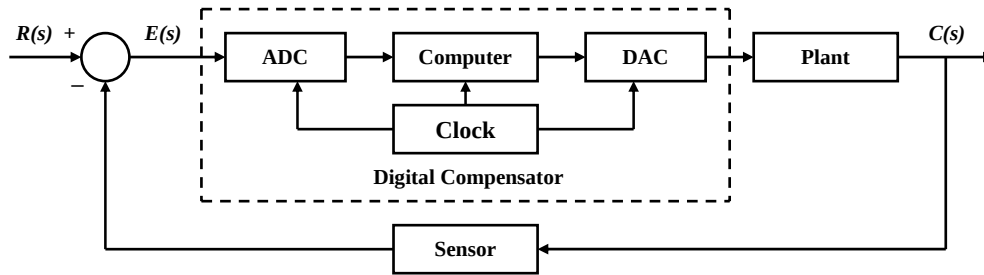


Figure 1. Basic Components of a Digital Control System

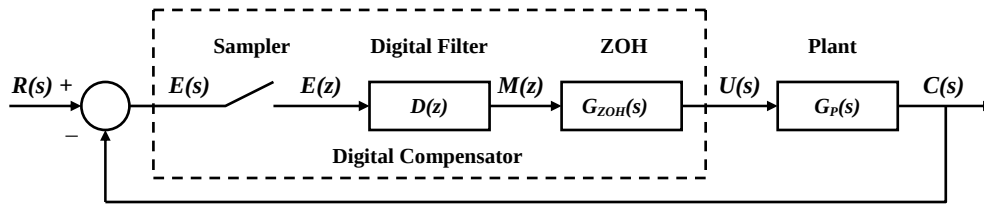


Figure 2. Modified Model of a Digital Control System

For the purpose of the analysis and design, the modified system model of Figure 2 will be further simplified by showing only the sampler, the zero-order hold circuit and the plant, as shown in Figure 3. After the system's analysis, a digital robust controller is designed in terms of its difference equations and is included in the structure of the system.

For reaching the objectives of this research, a complex real-time digital tracking control system is considered. Its continuous part is a high order plant that is preliminary reduced and approximated to a third order system. The block diagram shown at Figure 3, is not including currently the digital filter that is assumed as $D(z) = 1$ during the initial analysis of the system.

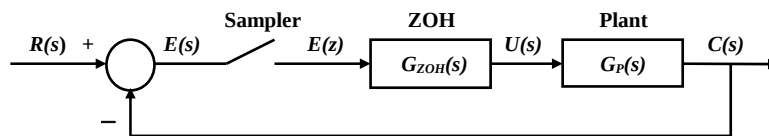


Figure 3. Further Simplified Digital Control System

3. D-Partitioning Analysis in the Discrete-Time Domain

The plant transfer function, as seen in Figure 3, is:

$$G_p(s) = \frac{K}{0.0004s^3 + 0.085s^2 + s} \quad (1)$$

The plant's gain K is considered as uncertain and variable system parameter. To determine the gain limitations and its marginal value, the method of the D-partitioning is applied in a similar manner like in its application for continuous systems [2, 6]. For the digital control system analysis, the D-partitioning is applied only to the



continuous plant, on condition that the sampling period T is taken into account. To achieve D-partitioning analysis accuracy, the sampling period T_s should be at least 10 times smaller than the plant's minimum time constant T_{min} , or $T_s \leq 0.1T_{min}$ [1, 6]. For the discussed system, the sampling period is chosen as $T_s = 0.0001\text{sec}$, while the plant's minimum time constant is $T_{min} = 0.005\text{sec}$. Following the steps of the D-partitioning analysis, the characteristic equation of the continuous plant $G_P(s)$ is presented as:

$$G(s) = 0.0004s^3 + 0.085s^2 + s + K = 0 \quad (2)$$

From equation (2), the variable parameter K of the original system is determined as:

$$K(s) = -0.0004s^3 - 0.085s^2 - s \quad (3)$$

The D-partitioning in terms of the parameter K , as seen in Figure 4, is achieved from equation (3) by following the procedure:

```
>> K=tf([-0.0004 -0.085 -1 0],[0 1])
>> nyquist(K)
```

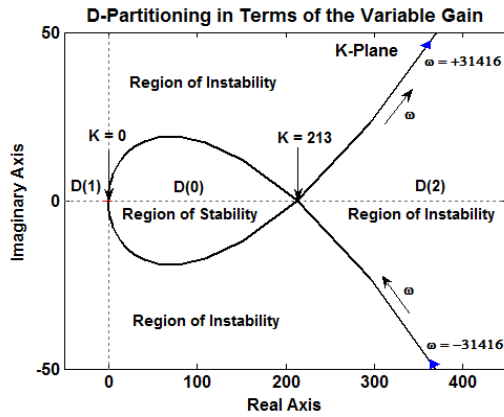


Figure 4. D-Partitioning Analysis of the System in Terms of the Variable Gain K

Taking into account that the frequency limits in the discrete-time domain are $\omega = \pm \omega_s/2 = \pm 2\pi/2T = \pm 31416$ rad/sec, the plotted D-partitioning curve in the K -plane is considered only within the range $-31416 \text{ rad/sec} \leq \omega \leq +31416 \text{ rad/sec}$ [7, 8].

The D-partitioning, as seen in Figure 4, determines three regions on the K -plane: $D(0)$, $D(1)$ and $D(2)$. Only $D(0)$ is the region of stability, being always on the left-hand side of the curve [2, 9]. This implies that the system is stable only when its gain is within the range $0 \leq K \leq 213$. At $K = 213$ the system becomes marginal. The result for the marginal system gain, obtained from the D-partitioning analysis can be confirmed in the discrete-time domain, Figure 5, in the following code:

```
>> Gp1=tf([0 213],[0.0004 0.085 1 0])
>> Hd1 = c2d(Gc1,0.0001,'zoh')
```

```
>> Hd1fb = feedback(Hd1,1)
Transfer function:
8.828e-008 z^2 + 3.513e-007 z + 8.735e-008
-----
z^3 - 2.979 z^2 + 2.958 z - 0.979
Sampling time: 0.0001
>> step(Hd1fb)
```

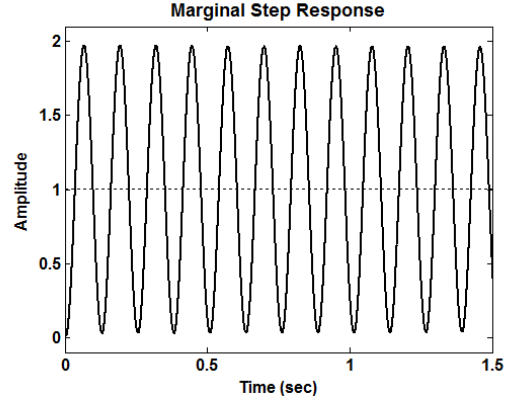


Figure 5. Marginal Step response for System Gain $K = 213$

The optimal continuous plant gain K at a relative damping ratio $\zeta = 0.707$ can be determined by taking into account the unity feedback of $G_P(s)$ and applying the following code [10]:

```
>> K=[0:0.001:10];
>> for n=1:length(K)
Gp_array(:,n)=tf([10000*K(n)],[4 850 10000 10000*K(n)]);
End
>> [y,z]=damp(Gp_array);
>> plot(K,z(1,:))
```

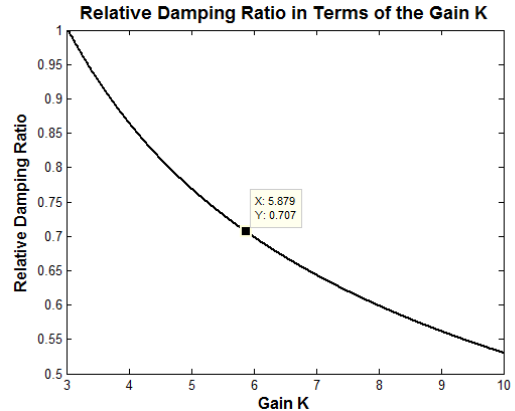


Figure 6. Determination of the Optimal Gain K

The relative damping ratio $\zeta = 0.707$ is achieved, at optimal continuous plant gain K . By interacting with the plot shown at Figure 6, the optimal gain is determined as $K = 5.879$. By applying the z-transform, the open-loop and closed-loop transfer functions of the system are determined in the discrete-time domain. The resulting time response to a step input signal is shown in Figure 7:

```
>> Gp2=tf([0 5.879],[0.0004 0.085 1 0])
>> Hd2 = c2d(Gp2,0.0001,'zoh')
>> Hd2fb = feedback(Hd2,1)
>> step(Hd2fb)
```



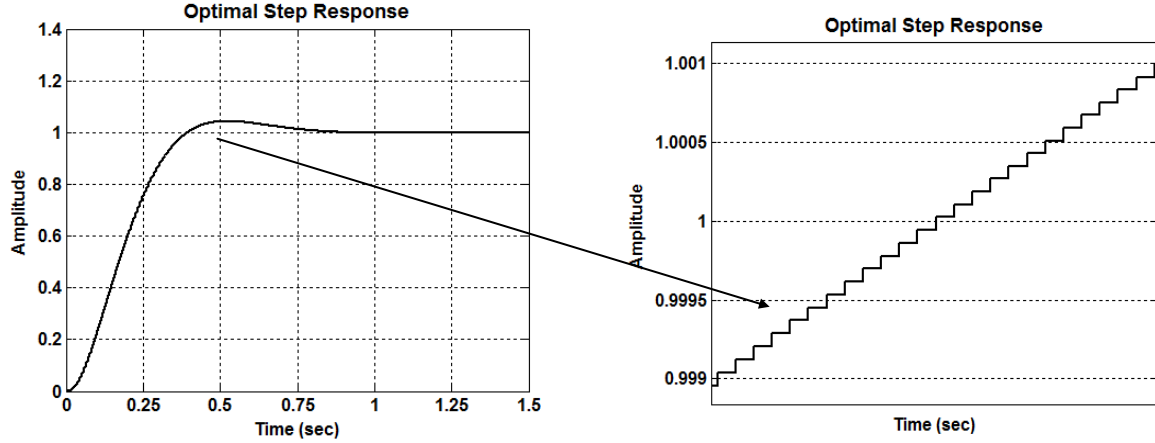


Figure 7. Step Response of the Optimal Digital System in Discrete-Time and its Zoomed-in Image

The digital system performance is quite sensitive to the variation of the gain K . This can be demonstrated with system's time response for different gain values as shown in Figure 8:

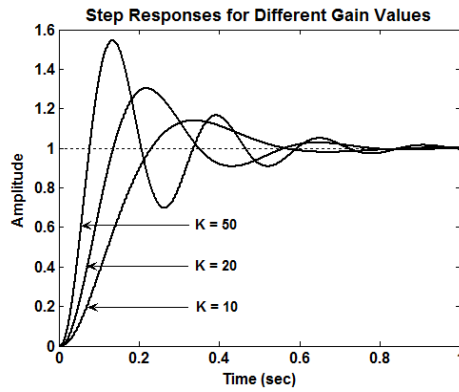


Figure 8. Step Responses of the Uncompensated Digital System in the Discrete-Time Domain

4. Robust Controller Design

The performance criterion "Integral of Time multiplied by the Absolute value of Error" (ITAE) is applied to achieve an optimal robust control. It is known that the ITAE reaches a minimum value for a relative damping ratio $\zeta = 0.707$ [3, 8, 9]. A solution, the system to meet the ITAE, is to implement a robust controller consisting of a series and a forward stage. The controller design employs a two-step compensation that operates in two degrees of freedom [10, 11, 12] and enforces a desired system damping, stability and time response.

The design strategy for constructing the series stage $G_S(s)$ of the controller is such that its two zeros are placed near the desired closed-loop poles satisfying the optimal system condition. The optimal value of K corresponding to the relative damping ratio $\zeta = 0.707$ of the closed-loop system is already determined in the previous section as $K = 5.879$. Then, the unity feedback transfer function of the plant can be presented also as:

$$G_{CL} = \frac{G_P(s)}{1 + G_P(s)} = \frac{10000K}{4s^3 + 850s^2 + 10000s + 10000K} \quad (4)$$

$$= \frac{58790}{4s^3 + 850s^2 + 10000s + 58790}$$

This condition corresponds to the desired closed-loop poles $-6.05 \pm j6.06$. They are determined by the code as shown below:

```
>> Gcl=tf([58790],[4 850 10000 58790])
>> damp(Gcl)
```

Eigenvalue	Damping	Freq. (rad/s)
$-6.05e+000 + 6.06e+000i$	7.07e-001	8.56e+000
$-6.05e+000 - 6.06e+000i$	7.07e-001	8.56e+000
$-2.00e+002$	1.00e+000	2.00e+002

After approximating the system pole values to $-6.1 \pm j6.1$, the two controller zeros can be placed at $-6.1 \pm j6.1$. For physical realization of the controller's series stage two remote controller poles, are added at $s_{1,2} = -1000, j0$, so that their effect on the system performance is negligible [3, 14, 15]. Thus, the transfer function of the series robust controller stage is presented as:

$$G_S(s) = \frac{(s + 6.1 + j6.1)(s + 6.1 - j6.1)}{74.42(s + 1000)^2} =$$

$$= \frac{s^2 + 12.2s + 74.42}{74.42(s + 1000)^2} \quad (5)$$

However, to simplify the analysis, the remote poles $s_{1,2} = -1000, j0$ are not included into the transfer function of the open-loop plant:

$$G_{OL}(s) = G_S(s)G_P(s) =$$

$$= \frac{0.01344K(s^2 + 12.2s + 74.42)}{0.0004s^3 + 0.085s^2 + s} \quad (6)$$

As a result, the unity feedback transfer function, including the series controller is:



$$G_{CL}(s) = \frac{0.01344K(s^2 + 12.2s + 74.42)}{0.0004s^3 + 0.085s^2 + s + 0.01344K(s^2 + 12.2s + 74.42)} \quad (7)$$

As seen from equation (7), the zeros of the closed-loop transfer function are in the vicinity of its poles. As a result, the closed-loop zeros will cancel the closed loop poles of the plant. To avoid this problem, a forward controller $G_F(s)$ [10, 16] is added in series to the closed-loop transfer function $G_{CL}(s)$, as shown in Figure 9.

The strategy for design of the forward stage $G_F(s)$ is such that its poles should cancel the zeros of the closed-loop plant transfer function $G_{CL}(s)$. As a result, the total compensated part of the system $G_T(s)$ will be dominated mainly by the two poles $-6.1 \pm j6.1$, optimizing its

performance. To improve on the step response rise time, a zero is added to the transfer function of the forward stage. Then, the transfer function of the forward controller stage is presented as:

$$G_F(s) = \frac{74.42(0.1s + 1)}{s^2 + 12.2s + 74.42} \quad (8)$$

The transfer function of the total compensated part, as seen from the block diagram in Figure 9, is presented as:

$$G_T(s) = G_F(s)G_{CL}(s) = \frac{K(0.1s + 1)}{0.0004s^3 + 0.085s^2 + s + 0.01344K(s^2 + 12.2s + 74.42)} \quad (9)$$

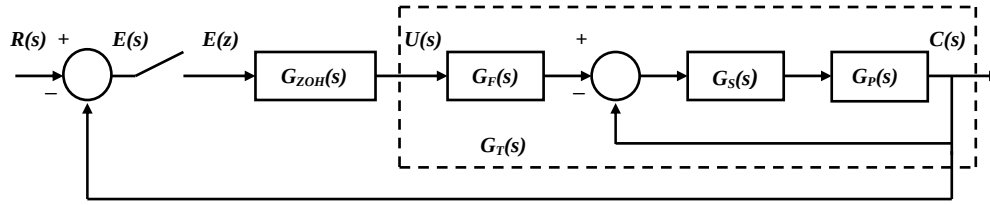


Figure 9. Analogue Robust Controller Incorporated into the Control System

The D-partitioning in terms of the variable parameter K is determined from the characteristic equation of $G_T(s)$ and is plotted in Figure 10 following the course of action:

$$K = \frac{-0.0004s^3 - 0.085s^2 - s}{0.0134s^2 + 0.164s + 1} \quad (10)$$

```
>> K = tf([-0.0004 -0.085 -1 0],[0.0134 0.164 1])
>> nyquist(K)
```

It is seen from Fig. 10 that the D-partitioning curve determines four regions of the K -plane: D(0), D(1), D(2) and D(3). Only D(0) is the region of stability, being always on the left-hand side of the curve for a frequency variation from $\omega = -31416$ to $\omega = +31416$. It is seen that the system will be stable for any values of $K > 0$, since these values are within the region D(0). Also, the compensated system is examined for robustness in the Discrete-time domain by substituting different random values for the gain, $K = 20, 50$ and 200 in equation (9):

$$G_{T1}(s)_{K=20} = \frac{20(0.1s + 1)}{0.0004s^3 + 0.085s^2 + s + 0.27(s^2 + 12.2s + 74.42)} = \frac{20(0.1s + 1)}{0.0004s^3 + 0.35s^2 + 4.28s + 20} \quad (11)$$

$$G_{T2}(s)_{K=50} = \frac{50(0.1s + 1)}{0.0004s^3 + 0.085s^2 + s + 0.67(s^2 + 12.2s + 74.42)} = \frac{50(0.1s + 1)}{0.0004s^3 + 0.76s^2 + 9.2s + 50} \quad (12)$$

$$G_{T3}(s)_{K=200} = \frac{200(0.1s + 1)}{0.0004s^3 + 0.085s^2 + s + 2.69(s^2 + 12.2s + 74.42)} = \frac{200(0.1s + 1)}{0.0004s^3 + 2.77s^2 + 33.7s + 200} \quad (13)$$

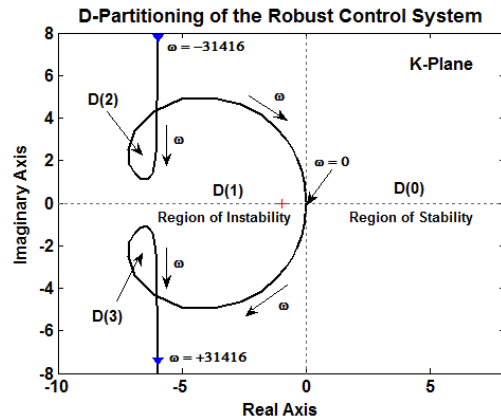


Figure 10. D-Partitioning of the Robust Control System in Terms of the Variable Gain K



The system performance in the discrete-time domain, as shown in Figure 11, is obtained by the following code:

```
GT1=tf([2 20],[0.0004 0.35 4.28 20])
GT2=tf([5 50],[0.0004 0.76 9.2 50])
GT3=tf([20 200],[0.0004 2.77 33.7 200])

Hd1 = c2d(GT1,0.0001,'zoh')
Hd2 = c2d(GT2,0.0001,'zoh')
Hd3 = c2d(GT3,0.0001,'zoh')

Hd1fb = feedback(Hd1,1)
Hd2fb = feedback(Hd2,1)
Hd3fb = feedback(Hd3,1)

step(Hd1fb,Hd2fb,Hd3fb)
```

It is seen from Figure 11 that as a result of the applied two-stage robust controller, the digital control system becomes quite insensitive and robust to variation of the gain K .

The step responses, shown in the discrete-time domain, for cases of $K = 20$, $K = 50$ and $K = 200$ differ insignificantly from each other.

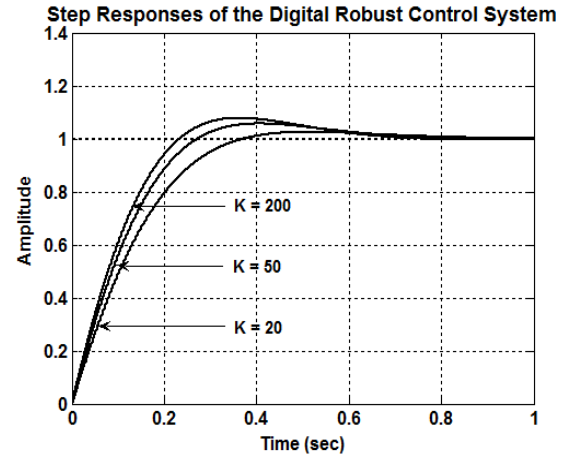


Figure 11. Step Responses of the Digital Robust Control System in the Discrete-Time Domain

damp(Hd1fb1)

Eigenvalue	Magnitude	Equiv. Damping	Equiv. Freq. (rad/s)
9.99e-001 + 4.46e-004i	9.99e-001	8.10e-001	7.61e+000
9.99e-001 - 4.46e-004i	9.99e-001	8.10e-001	7.61e+000
9.17e-001	9.17e-001	1.00e+000	8.63e+002

damp(Hd1fb2)

Eigenvalue	Magnitude	Equiv. Damping	Equiv. Freq. (rad/s)
9.99e-001 + 5.41e-004i	9.99e-001	7.46e-001	8.14e+000
9.99e-001 - 5.41e-004i	9.99e-001	7.46e-001	8.14e+000
8.28e-001	8.28e-001	1.00e+000	1.89e+003

damp(Hd1fb3)

Eigenvalue	Magnitude	Equiv. Damping	Equiv. Freq. (rad/s)
9.99e-001 + 5.93e-004i	9.99e-001	7.16e-001	8.50e+000
9.99e-001 - 5.93e-004i	9.99e-001	7.16e-001	8.50e+000
5.01e-001	5.01e-001	1.00e+000	6.91e+003

As seen from the results, the digital system has become quite robust. In spite of the considerable change of the gain, from $K = 20$, to $K = 50$ and further to $K = 200$, the system's dominant conjugative poles and the system's relative damping ratio change insignificantly.

One of the approaches used to realize the series and forward robust controller stages in analogue systems is to implement RC networks and operational amplifiers [8, 17, 18].

Alternatively, digital filters corresponding to the two robust stages can be designed and implemented in the system.

Taking into account equation (5), the transfer function of the series digital robust stage can be determined by:

```
>> Gs=tf([1 12.2 74.42],[74.42 148840 74420000])
>> Ds = c2d(Gs,0.0001,'zoh')
Transfer function:
0.01344 z^2 - 0.0268 z + 0.01336
-----
z^2 - 1.81 z + 0.8187
Sampling time: 0.0001
```

To enable the implementation of a microcontroller in the mini-loop of the system, the transfer function of the series digital robust stage $D_s(z)$ is expressed in the discrete-time domain [19, 20, 21] by the following difference equation:

$$y(kT) = 0.01344x(kT) - 0.0268x[(k-1)T] + 0.01336x[(k-2)T] + 1.81y[(k-1)T] - 0.8187y[(k-2)T] \quad (14)$$

Similarly, from equation (8), the transfer function of the forward digital filter stage is determined by:

```
>> Gf=tf([7.442 74.42],[1 12.2 74.42])
>> Df = c2d(Gf,0.0001,'zoh')
Transfer function:
0.0007441 z - 0.0007434
-----
z^2 - 1.999 z + 0.9988
Sampling time: 0.0001
```

To enable the implementation of a microcontroller in series with the mini-loop of the system the transfer function of the forward digital robust stage $D_F(z)$ is expressed in the discrete-time domain [19, 20, 21] by the following difference equation:



$$m(kT) = 0.0007441e[(k-1)T] - 0.0007434e[(k-2)T] + 1.999m[(k-1)T] - 0.9988m[(k-2)T] \quad (15)$$

The block diagram of Figure 9 is modified to show the operation of the robust control system in the discrete-time domain, as seen in Figure 12.

Each combination of a sampler, a digital filter $D_F(z)$ or $D_S(z)$, solving a difference equation, plus a zero-order hold (ZOH), can be represented by a combination of

the ADC, microcontroller and DAC, as shown in Figure 13. It is seen that the two microcontrollers, representing the series and the forward stages are incorporated into the control system. They are accordingly programmed to solve the difference equations (14) and (15) [6, 22].

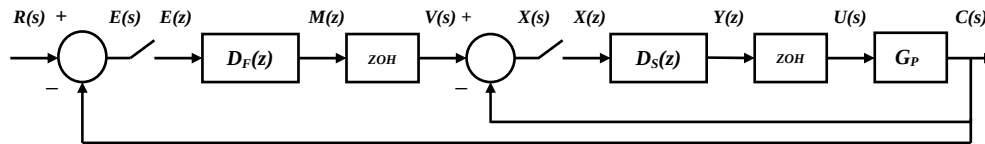


Figure 12. Two-Stage Digital Robust Controller Incorporated into the Control System

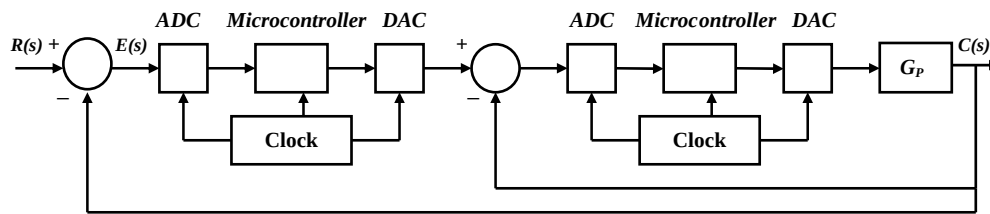


Figure 13. Two-Stage Robust Microcontroller Incorporated into the Control System

5. Conclusions

Contributions of this research are the advancement of the D-partitioning method in its application for analysis of digital control systems and digital robust control design. The results of the research demonstrate that the D-partitioning analysis can be applied to the continuous plant in a digital control system, if the system's sampling period is at least 10 times smaller than the plant's minimum time constant. If this condition is not met, the accuracy of the D-partitioning analysis is to some extent affected, but the method still can be applied with a reasonable effect.

With the aid of the D-partitioning analysis, the marginal conditions of the digital system are determined and further the results are confirmed by the system's step response in the discrete-time domain.

The original digital control system is examined for robustness. It is established that the system is quite sensitive to any changes of the plant's gain. Based on the ITAE criterion, a design strategy of a robust controller is suggested.

An optimal robust controller is achieved by applying forward-series compensation with two degrees of freedom. The controller enforces a desired system damping and therefore a desired margin of stability. The system is analyzed with the robust controller and the

results prove that it becomes quite insensitive to the gain variations.

Following the robust controller design, two options of its implementation are suggested. One of the solutions is to construct the series and forward controller stages with the aid of RC circuits and operational amplifiers and applied them in the system as analogue continuous components. Alternatively, the transfer functions of the two controller stages are presented in the discrete-time domain by their difference equations.

Two microcontrollers are incorporated into the control system as series and forward robust control stages and are programmed to solve their difference equations. Due to the advantages associate with the digital systems, the microcontroller robust design strategy is the better option and can be applied for any control system.

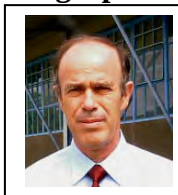
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Biographies



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Trends in Region 3 Control System Performance as Wind Turbine Size Increases

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Abstract

Wind turbines are complex, nonlinear systems subjected to large loads that reduce performance and lifetime. To ameliorate this, high performance control systems must be implemented. Plant characteristics such as flexible body dynamics and nonminimum phase reduce available control performance as their size increases. A comparative analysis of available control performance for a 600 kW and a 5 MW wind turbine is presented. Control limitations for each are identified, and available feedback is quantified. Dimensionless parameters are defined to provide response-based measures for comparison. Results of nonlinear simulations for both wind turbines are presented and compared. Aggressive control strategies are developed in an attempt to improve the performance of the larger wind turbine's controllers.

Keywords: *Wind energy, Region 3 Control, Speed regulation, Vibration suppression, Single-input/Two-output, Variable gain, Nyquist-stable control.*

1 Introduction

Wind turbines are nonlinear systems, with characteristics that substantially restrict the available control system feedback: variable plant parameters, nonminimum phase zeros, adjacent pole pairs at low frequency, actuator delay and other limiting factors. Control performance, particularly in Region 3 (beyond rated wind speed), is critical, as the forces applied can severely reduce turbine lifetime.

Wind turbine Region 3 rate regulation and load mitigation control has been extensively researched. Model-based gain selection strategies for PID Region 3 compensators have been developed [1, 3]. Control of hybrid renewable energy systems are reported in [2]. Researchers at the National Renewable Energy

Center (NREL) have designed and implemented advanced control systems for rotor rate regulation and load mitigation applied to the Controls Advanced Research Turbine 2 (CART2), a two-bladed 600 kW Westinghouse wind turbine, for comparison to fixed gain low order controllers. Independent blade pitch control strategies to mitigate the effect of asymmetric wind variations across the rotor disk implemented on CART2 are reported in [4, 5]. Combined collective and individual blade pitch control for load mitigation is investigated in [6]. H_{inf} control is used for speed and tower damping [7]. In [8], a Linear Quadratic Gaussian design with novel refinements is used to account for CART2 actuator delay. State-space control with disturbance accommodation is investigated in [9, 10, 11]. Full-state feedback is used for tower damping and torsion mode control for NREL's three-bladed CART3 turbine in [12].

The US Department of Energy suggests wind power capacity will increase by roughly an order of magnitude (305 GW) by 2030 [13]. To meet this goal, deployed turbines must be substantially larger than what are deployed today. In [14], it is stated that 5 MW turbines or larger are needed to meet the goals of the 2030 report. These turbines will have hub heights of 140 m and 153 m rotor diameters. Comparing this the 80 m hub heights and 77 m rotor diameters of currently deployed 1.5 MW turbines prompts two questions related to automatic control applications for these plants: what control performance is required for these 5 MW turbines, and what control performance is available?

For a constant torque wind turbine, Region 3 control has the primary goal of generator rate regulation. A low variance of this output is desired. Upper bounds on rate amplitude also apply subject to the limitations of the turbine power electronics (e.g. for CART2, a high speed shaft rate greater than 1860 rpm is considered an overspeed event (rated speed is 1800 rpm)). Ancillary Region 3 goals are related to



load mitigation. This includes using variable blade pitch to damp tower modes, and the use of generator torque to damp the shaft torsion mode.

Control performance is directly proportional to feedback, which in turn is a function of the available control bandwidth. Some plant characteristics, like flexible body dynamics and nonminimum phase, have the effect of reducing available bandwidth. It is expected that the flexible modes of proposed very large wind turbines (5 MW and larger) will be at lower frequencies than those of current turbines, and thus the available feedback is less. It is expected that the maximum blade angle rates will be lower than those of smaller turbines, also limiting the available control performance due to saturation. As a consequence, it is expected that the available control performance for the 5 MW turbines is less than that of current systems.

While effective, many of these control approaches make difficult the task of *quantifying* achievable performance. Modifying state or control penalties of an LQG controller effect changes in closed loop performance, but it is difficult to relate these changes to effects on performance or robustness in a quantifiable way. Adaptive control strategies can force system response to match that of a desired system, but there are potential robustness issues and it is not clear what the effect on disturbance rejection is. In this work, a comparative analysis is presented, quantifying the available control performance for the CART2 turbine and the 5 MW offshore wind turbine. Classical control methods are employed for two purposes: these allow full use of plant frequency response data, and the control performance is easily quantifiable. The purpose of this paper is not to argue the relative merits of different control approaches as they relate to wind turbine applications, but to investigate trends in available control performance as these systems grow in size.

The paper is organized as follows: the Region 3 control architecture is presented, the available feedback for CART2 and the 5 MW wind turbine is quantified, the single input/two output control designs are detailed, and control simulation results using NREL's Fatigue, Aerodynamics, Structure and Turbulence (FAST) models [15] for both systems are reported to illustrate a trend in control performance as wind turbine size increases. A discussion of aggressive control strategies for possible application to large wind turbines precedes the conclusions.

Nomenclature. The rational function $T(s)$ of the Laplace variable s is the loop transmission (alternatively return ratio) of a feedback loop. $|F(s)| = |1 + T(s)|$ is the feedback. $|F(s)| > 1$, $|F(s)| < 1$ and $|T(s)| \ll 0$ define *negative*, *positive* and *negligible* feedback, respectively. These definitions are rooted in the effect of feedback on the logarithmic response of

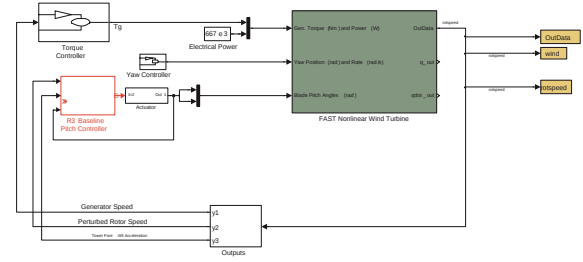


Figure 1: Region 3 control block diagram. Torque Controller 1 is the Single Input/Single Output (SISO) system, R3 Pitch Controller is the Single Input/Two Output (SITO) system.

the closed loop system to disturbances. For a low pass loop transmission, ω_b where $|T(j\omega_b)| = 1$ is the *bandwidth* (alternatively *0 dB crossover frequency*), and ω_f where $|T(j\omega)| = A_0$, $\forall \omega \leq \omega_f$, A_0 a constant is the *functional bandwidth*. $f = \frac{\omega}{\omega_f}$, for ω in the set of nonnegative real numbers. *Nonminimum phase* is the phase lag not found using the Bode phase/gain relationship [16].

2 Control Architecture

The goals of the control design are twofold. The primary goal is turbine shaft rate regulation in Region 3 (CART2 is a constant torque wind turbine, and for purposes of this comparison, the 5 MW wind turbine is assumed to be constant torque as well). Second, flexible body modes are targeted for feedback application for load mitigation. The control variables are collective blade pitch and generator torque. The outputs are high speed shaft rate and tower fore/aft acceleration. These are variables in two control systems: a single-input/single-output controller (generator torque input and high speed shaft rate output), and a single-input/two-output controller (collective blade pitch input, and high speed shaft rate and tower fore/aft acceleration are outputs). Figure 1 shows the block diagram for this controller.

The generator and the high speed shaft are collocated, and the frequency responses for both the CART2 (figure 2) and the 5 MW turbine consist of alternating zero/pole pairs that facilitate high performance feedback design due to phase constraints. Low order feedback compensation for both turbines results in more than 20 dB attenuation of shaft vibrations at the modal frequency. This control has the added benefit of reducing the modulus of the high speed shaft response to collective blade pitch, making easier the task of gain stabilization of that mode in the shaft rate regulation design problem.

The design of the collective blade pitch controller is more challenging, and is the focus of this comparative analysis. The plant is single-input/two-output



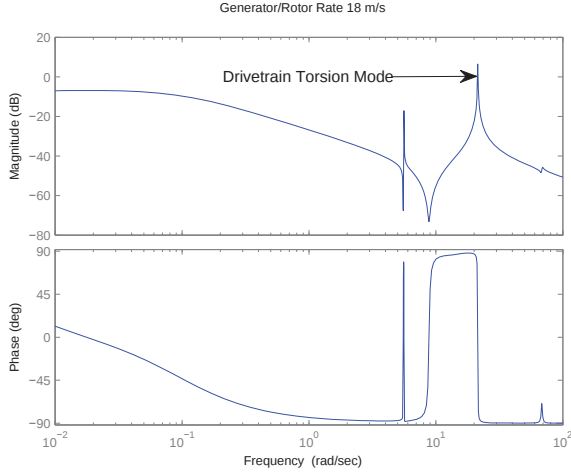


Figure 2: CART2 Generator Torque to High Speed Shaft Rate Frequency Response at 18 m/s

(SITO); the two outputs are high-speed shaft rate for the Region 3 regulation problem (primary), and tower fore/aft acceleration for the vibration suppression problem (secondary). In contrast to the generator/high speed shaft rate frequency response, the collective blade pitch responses present challenging design problems and it is this controller that distinguishes the two turbines from the perspective of available performance.

The comparative analysis is presented thusly:

1. Limitations to available feedback for the SITO collective blade pitch controller are identified and quantified for both the CART2 and 5 MW wind turbines.
2. The same loop transmission shape is applied to both the CART2 and 5 MW high speed shaft rate control design that produces robust, high performance, linear control.
3. Using 1 and 2, the available feedback over a chosen functional bandwidth for both turbine control designs is calculated and compared.
4. Controller designs for both systems are developed, and the performance is compared using FAST simulations of both wind turbines. These results are compared to the trend indicated in 3.

It is assumed that adequate driveshaft vibration attenuation is achieved via simple generator torque control, and details of this design are not included in the comparative analysis.

3 Comparison of Available Feedback: CART2 vs 5 MW

The performance difference between the collective blade pitch control performance of the 600 kW and 5 MW wind turbines is quantified. The following conditions apply to both systems.

1. The plant is two output: high speed shaft rate, and tower fore/aft acceleration.
2. The control system is linear.
3. The blade pitch rate is limited. 20 deg/s for CART2, 8 deg/s for the 5 MW turbine.
4. The functional bandwidth, defined as the frequency to which the loop transmission has a constant magnitude A_0 , is 0.1 rad/s. This allows more than one decade for the loop transmission roll off.
5. The loop transmission roll off at frequencies higher than the functional bandwidth is -10 dB/oct. This approach combines aggressive feedback delivery with good robustness (-150 degree phase after the break).
6. If there are x octaves between the functional bandwidth and the 0 dB crossover frequency, then the loop transmission modulus over the functional bandwidth is $10(x+1)$ dB. This is additional 10 dB of feedback is extracted by careful loop shaping at the functional bandwidth break and near 0 dB crossover.

The desired loop transmission function is given by the following function [16].

$$T_{des} = e^{\log(A_0) + \frac{5}{3}\log\theta(jf)}, \quad (1)$$

where $\theta(jf) = \frac{1}{\sqrt{1-f^2+jf}}$, and f is the frequency normalized with respect to functional bandwidth. Figure 3 shows the modulus and argument of this function. Implemented controllers will be defined by rational functions with poles and zeros selected to approximate this shape.

3.1 Available Feedback for the CART2 Region 3 High Speed Shaft Rate Regulator

Figures 4 and 5 show the CART2 high speed shaft rate and tower fore/aft acceleration responses to collective blade pitch at 18 m/s wind speed, found using the FAST model of this turbine. These plots are used to determine the feedback limitations for the high speed shaft rate controller, the primary controller for this application.



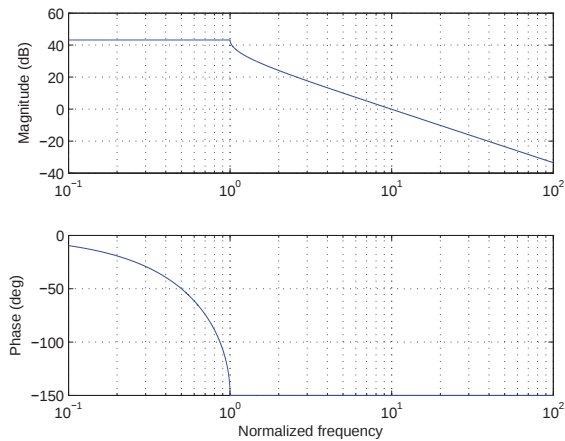


Figure 3: Desired loop transmission as a function of frequency normalized with respect to functional bandwidth.

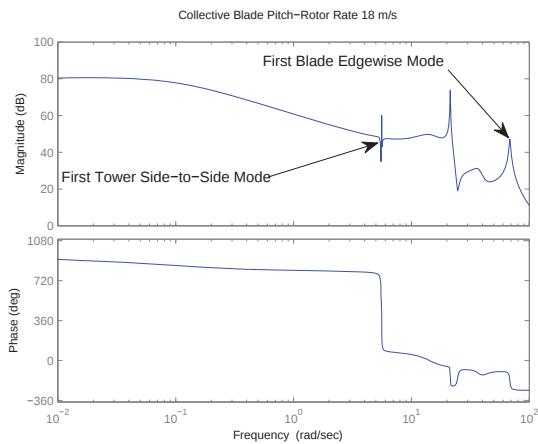


Figure 4: CART 2 Collective Blade Pitch to High Speed Shaft Rate Frequency Response at 18 m/s

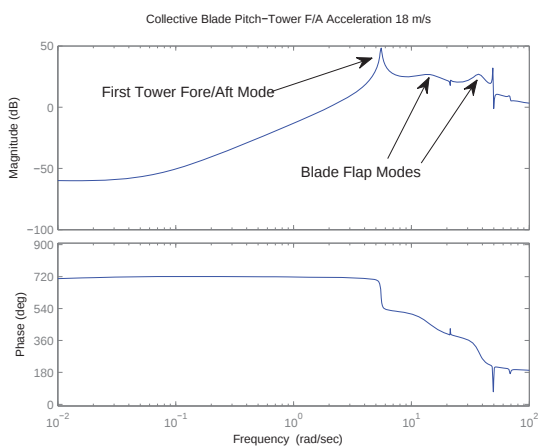


Figure 5: CART2 Collective Blade Pitch to Tower Fore/Aft Acceleration Frequency Response at 18 m/s

1. There is substantial nonminimum phase in the neighborhood of the first tower mode at 5.5 rad/s of the shaft rate response (figure 4). This is the result of right-half plane zeros clustered near the first tower fore/aft and side-to-side modes at this frequency. The nonminimum phase delay at this frequency makes impossible the design of a shaft rate controller of higher bandwidth.
2. There must be at least one octave of separation between the frequencies where the high speed shaft rate is negative and where the acceleration feedback is negative.
3. The first blade edgewise mode at about 70 rad/s in the shaft rate response must be either gain or phase stabilized. The drivetrain mode at 22 rad/s is reduced by the generator controller, however the collective blade pitch controller will stabilize this mode in case the generator loop is opened.

Items 1 and 2 are the most substantial limitations. The nonminimum phase at 5.5 rad/s described in item 1 requires gain stabilization of modes near this frequency. This limits the bandwidth of the high speed shaft rate controller to ~ 1 -2 rad/s. Control of the 1st tower fore/aft mode (5.5 rad/s) requires acceleration feedback over the interval [4 6] rad/s, thus the restriction of item 2 limits the high speed rate feedback loop to 2 rad/s. The bandwidth limit caused by item 3 is higher than those of 1 and 2, and thus is not a critical limitation.

The crossover frequency is ~ 1.5 rad/s. The interval from the functional bandwidth (0.1 rad/s) to the crossover frequency of 1.5 rad/s is 3.9 octaves wide. Assuming careful loop shaping at the break, the high speed shaft controller can deliver $10(3.9 + 1) = 49$ dB of feedback over the functional bandwidth.

3.2 Available Feedback for the 5 MW Region 3 High Speed Shaft Rate Regulator

Figures 6 and 7 show the 5 MW high speed shaft rate and tower fore/aft acceleration responses to collective blade pitch at 18 m/s wind speed. These plots are used to list the feedback limitations for the high speed shaft rate controller.

1. There is substantial nonminimum phase in the neighborhood of the first tower mode at 2 rad/s of the shaft rate response (figure 6). This is the result of right-half plane zeros clustered near the first tower fore/aft and side-to-side modes at this frequency. The nonminimum phase delay at this frequency makes impossible the design of a shaft rate controller of higher bandwidth.



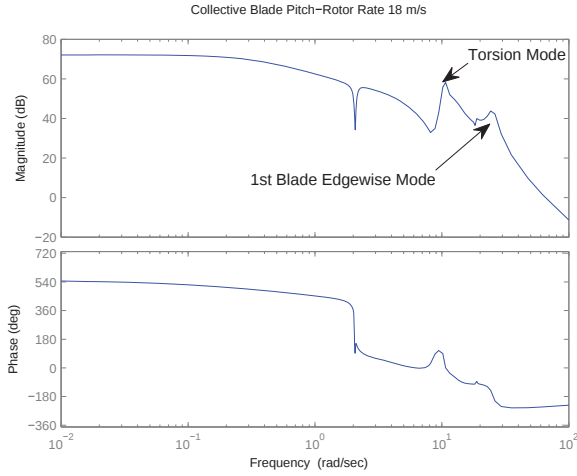


Figure 6: 5 MW Collective Blade Pitch to High Speed Shaft Rate Frequency Response at 18 m/s

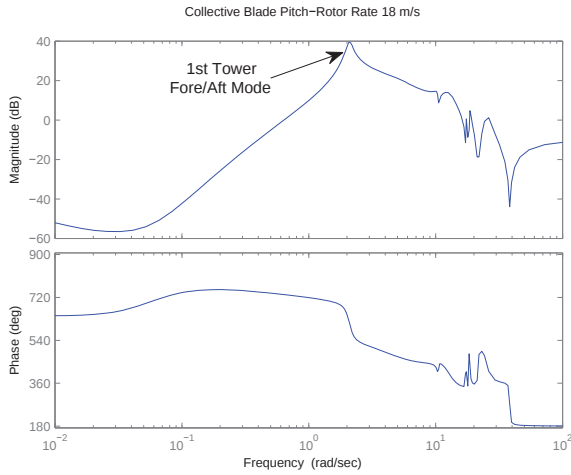


Figure 7: 5 MW Collective Blade Pitch to Tower Fore/Aft Acceleration Frequency Response at 18 m/s

2. There must be at least one octave of separation between the frequencies where the high speed shaft rate is negative.
3. The first blade edgewise mode at about 25 rad/s in the shaft rate response must be stabilized (either gain or phase). The drivetrain mode at 10 rad/s is attenuated by the generator controller, however the collective blade pitch controller will stabilize this mode in case the generator loop is opened.

As is the case for CART2, items 1 and 2 are the most substantial limitations. While this response does not have the sharp poles that CART2 has around 2 rad/s, the nonminimum phase at this frequency described in item 1 requires gain stabilization of modes near this frequency. This limits the bandwidth of the high speed shaft rate controller to $\sim 0.5 - 1$ rad/s. Con-

trol of the 1st tower fore/aft mode (2 rad/s) requires acceleration feedback over the interval $\sim [1 \ 4]$ rad/s, thus the restriction of item 2 limits the high speed rate feedback loop to 0.5 rad/s. The driveshaft and edgewise modes are sufficiently higher frequency than the limits of items 1 and 2 that item 3 is not a limitation.

The crossover frequency is ~ 0.5 rad/s. The interval from the functional bandwidth (0.1 rad/s) to the crossover frequency of 2 rad/s is 2.3 octaves wide. Assuming careful loop shaping at the break, the high speed shaft controller can deliver $10(2.3 + 1) = 33$ dB of feedback over the functional bandwidth.

The more substantial limitations of the 5 MW turbine result in a factor of approximately six times less rate regulation performance than the CART2, theoretically.

4 SITO Designs for CART2 and 5 MW

Single-input/single-output (SISO) generator controllers are designed for each turbine to damp the shaft torsion mode. These are simple controllers and are not described in this work. The collective blade pitch SITO designs are subject to the bandwidth limitations quantified in the previous sections, and classical loop shaping methods are employed to extract maximum feedback over the functional bandwidth. It is noted that although the controllers are designed using linearized dynamic models at a fixed wind speed, they will be tested in turbulent wind conditions over a broad interval of Region 3 speeds. The compensators for the CART2 SITO collective blade pitch controller (high speed shaft rate $G_{rate(C2)}(s)$ and structure control $G_{struct(C2)}(s)$) are

$$G_{rate(C2)}(s) = \frac{0.1(s^2 + s + 0.25)}{s^4 + 20.1s^3 + 102s^2 + 10.2s + 1} \quad (2)$$

$$G_{struct(C2)}(s) = \frac{0.3}{s + 6}. \quad (3)$$

The compensators for the 5 MW SITO collective blade pitch controller (high speed shaft rate $G_r(s)$ and structure control $G_{struct(5)}(s)$) are

$$G_r(s) = \frac{0.09(s^3 + 3.5s^2 + 3.5s + 1)}{s^5 + 60.1s^4 + 531s^3 + 1303s^2 + 130.3s + 12.5} \quad (4)$$

$$G_{struct(5)}(s) = \frac{0.9525(s^2 + 19s + 84)}{s^3 + 58s^2 + 416s + 800}. \quad (5)$$

The loop transmissions for these controllers are shown in figure 8. The first 0 dB crossover frequencies and functional bandwidth feedback are consistent with the projected maximum values determined in



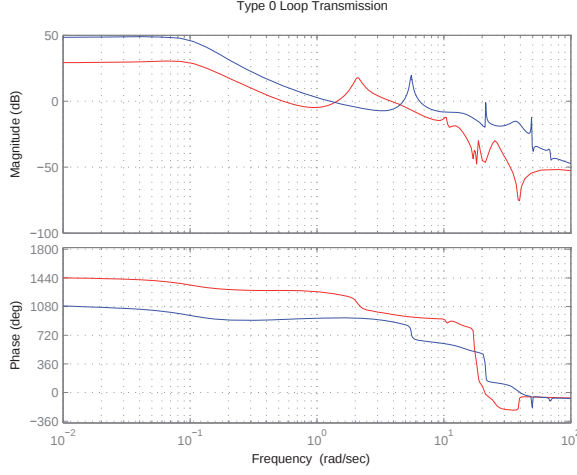


Figure 8: Region 3 Collective Blade Pitch SITO Loop Transmissions for the CART2 (blue) and 5 MW (red) wind turbines at 18 m/s

section 3. The relative stability of both controllers are comparable; there is approximately 6 dB of gain stability margin and 30 degrees of phase stability margin for both controllers.

5 Control Performance with Turbulent Wind

The analysis of the previous section is tested in simulation using FAST models of these two turbines in turbulent wind environments, where wind speeds vary between 10 and 27 m/s. The loop transmission functions are slightly modified to include origin poles for asymptotic rejection of step wind disturbances (see figure 9). It is noted that while there no longer is a functional bandwidth for these controllers, the difference in loop transmission modulus at 0.1 rad/s is the same as what is reported in section 3.

To quantify the relative performance of the SITO controllers, two dimensionless parameters are defined. The high speed shaft rate standard deviation to rated speed ratio (*SDRS*) is

$$SDRS = \frac{\sigma_{hss}}{\omega_{hss(rated)}}, \quad (6)$$

where σ_{hss} and $\omega_{hss(rated)}$ are the high speed shaft rate standard deviation and the Region 3 rated high speed shaft rate, respectively, in the same units. As the turbines in this study are constant torque, *SDRS* provides a measure of power variation in Region 3.

The secondary goal of the collective blade pitch SITO controller is tower fore/aft acceleration attenuation. The tower fore/aft acceleration ratio (TFAAR) is

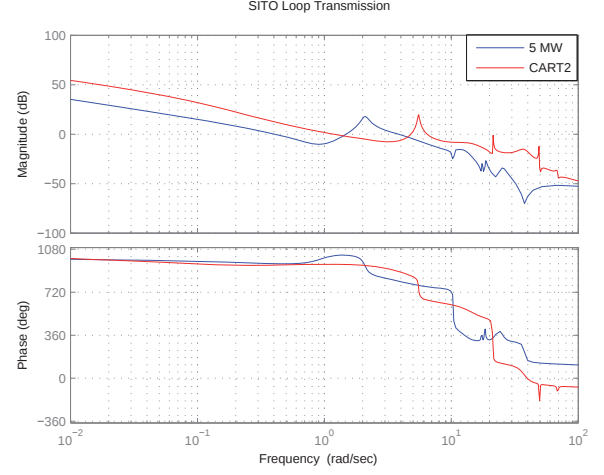


Figure 9: Type 1 Region 3 Collective Blade Pitch SITO Loop Transmissions for the CART2 (red) and 5 MW (blue) wind turbines at 18 m/s

Table 1: SITO Control Performance in Turbulent Wind

	CART2	5MW
Rated Shaft Speed (rpm)	1800	1174
HSS rate std (rpm)	31.9	83.46
Tower Acc std (ol) (m/s/s)	0.3549	0.2988
Tower Acc std (cl) (m/s/s)	0.2193	0.1719

$$TFAAR = \frac{\sigma_{a(cl)}}{\sigma_{a(ol)}}, \quad (7)$$

where $\sigma_{a(cl)}$ is the tower fore/aft acceleration standard deviation with the SITO controller closed, and $\sigma_{a(ol)}$ is the tower fore/aft acceleration standard deviation with high speed shaft rate feedback only (referred to as the *baseline*).

FAST simulations are performed for both turbines over 10 minutes in turbulent wind. Table 1 shows the rated high speed shaft rate, the high speed shaft rate standard deviation, and tower fore/aft acceleration in open and closed loop conditions for both turbines with the SITO controllers with loop transmissions shown in figure 9.

The TFAAR for the the CART2 and 5 MW turbines are 0.62 and 0.58, respectively. The performance is comparable, which is consistent with the fact that both controllers apply close to the same feedback at the tower first mode frequency. The *SDRS* for the the CART2 and 5 MW turbines are 0.018 and 0.071, respectively. The 5 MW *SDRS* is four times greater than that of CART2. This is consequence of the 5 MW wind turbine plant dynamics restricting available feedback to greater effect than for the CART2 turbine. As such, alternative methods, perhaps including pas-



sive damping, may be indicated for structural quieting of the 5 MW turbine.

6 Aggressive Feedback Strategies for 5 MW SITO Designs

The 5 MW wind turbine plant dynamics severely restrict available feedback, which is quantified in section 3. These results are consistent with other simulations with step wind changes, and more severe turbulent wind [17]. Two advanced control strategies are investigated in this section in an attempt to improve the performance despite these limitations.

6.1 HSS Rate Error Variable Gain SITO Controller

The requisite frequency separation for the SITO collective blade pitch controller (item 2 from 3.2) is a substantial limitation. As rotor rate regulation is the primary function of the collective blade pitch controller in Region 3, a variable gain strategy is proposed to apply more rate feedback when error is large. This comes at the expense of lowering tower acceleration feedback to allow increased rate feedback bandwidth.

The variable gain algorithm is a function of high speed shaft rate (ω_{hss}) = 1174 rpm and gear ratio $r_g = 97$. The output is a gain applied to the nominal tower acceleration feedback compensator, $k_{struct(5v)}(\omega_{hss})$ and the nominal high speed shaft rate feedback compensator, $k_{rate(5v)}(\omega_{hss})$. Variable gain $k_{struct(5v)}(\omega_{hss})$ is defined in table 2. The constants $\omega_{hss(i)}$, $i = 1, 2, 3, 4$ are threshold values. If the high speed shaft rate is in the interval bounded by $\omega_{hss(2)}$ and $\omega_{hss(3)}$, the gain $k_{struct(5v)}(\omega_{hss})$ is unity, and the collective blade pitch controller is the SITO controller previously described. If the shaft speed is outside this interval, $k_{struct(5v)}(\omega_{hss})$ is reduced linearly until the shaft speed is outside the interval bounded by $\omega_{hss(1)}$ and $\omega_{hss(4)}$ where this gain is fixed at 0.05. Given this gain, the rate regulator gain is $k_{rate(5v)}(\omega_{hss}) = -4k_{struct(5v)}(\omega_{hss}) + 5$.

For the 5 MW wind turbine, the threshold values are $\omega_{hss(i)}$ are 1039, 1125, 1222, 1309 rpm for $i = 1, 2, 3, 4$. Figure 10 shows the loop transmission of the variable gain controller with different high speed shaft rate errors. The loop transmission associated with the largest rate error (light blue) has negligible feedback at the first tower mode frequency (approximately 2 rad/s), and has about one octave more bandwidth (1 rad/s) than the nominal SITO controller (0.4 rad/s). It is noted that the gain changes must be slower than the dynamics of the plant. A low pass filter at the output of the the variable gain system can be used to adequately slow the gain change [18].

Figures 11 and 12 show the high speed shaft rate for the 5 MW turbine with high speed shaft rate feed-

Table 2: HSS Rate Error Variable Gain Algorithm

ω_{hss}	$k_{struct(5v)}(\omega_{hss})$
$\omega_{hss} \leq \omega_{hss(1)}$	0.05
$\omega_{hss(1)} < \omega_{hss} \leq \omega_{hss(2)}$	$\frac{1}{r_g}(\omega_{hss} - \omega_{hss(1)})$
$\omega_{hss(2)} < \omega_{hss} \leq \omega_{hss(3)}$	1.0
$\omega_{hss(3)} < \omega_{hss} \leq \omega_{hss(4)}$	$-\frac{1}{r_g}(\omega_{hss} - \omega_{hss(3)}) + 1$
$\omega_{hss} > \omega_{hss(4)}$	0.05

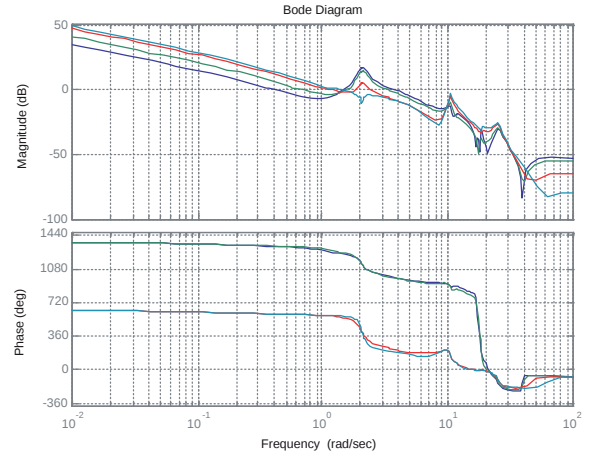


Figure 10: Variable Gain 5 MW SITO Loop Transmission. Dark blue is the standard SITO loop transmission. Green, red and light blue loop transmission are in conditions of increasing high speed shaft rate error.

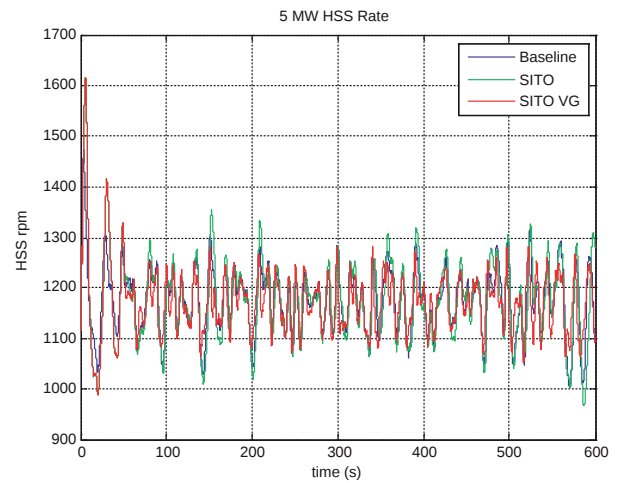


Figure 11: 5 MW high speed shaft rate for baseline (blue), fixed gain SITO (green) and variable gain SITO (red) collective blade pitch control.



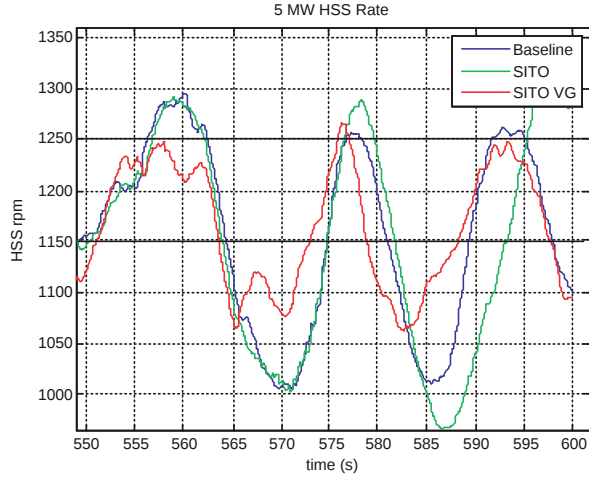


Figure 12: 5MW high speed shaft rate for baseline, fixed gain and variable gain collective blade pitch control. 1250 rpm is the variable gain threshold value.

back only (baseline), fixed gain SITO and variable gain SITO control in turbulent wind conditions. It is evident that variable gain system has superior performance compared to the fixed gain SITO controller. The SDRS for the variable gain controller is 0.0575, a 19% improvement over the fixed SITO controller. Figure 13 shows the tower fore/aft acceleration for the same controllers. As the acceleration feedback is reduced as a function of rate error, the variable gain controller has inferior performance to the fixed gain SITO system for tower load mitigation, but clearly superior to the baseline controller. The TFAAR for the variable gain controller is 0.67, a 16% reduction in performance compared to the fixed gain controller. This relative performance is a consequence of the variable gain strategy reducing the tower mode feedback when shaft rate error exceeds the threshold.

6.2 Nyquist-stable Controller

Another alternative to improve shaft rate regulation subject to the bandwidth limitations quantified in section 3 is to have a steeper roll off to the crossover frequency (i.e. roll off steeper than -10 dB/oct). This provides greater negative feedback over the functional bandwidth. A *Nyquist-stable* control system is one that is stable in closed loop, and has a stable loop transmission function that crosses the negative real axis of the Nyquist plot outside the unit circle. This describes a loop transmission function that over an interval of frequencies has a modulus slope steeper than -12 dB/oct. Nyquist-stable controllers have been implemented on aerospace plants with excellent results [19, 20]. The tradeoff for this increased feedback is a susceptibility to oscillation in actuator saturation. Nonlinear dynamic compensation can be employed with a Nyquist-stable system to satisfy the conditions

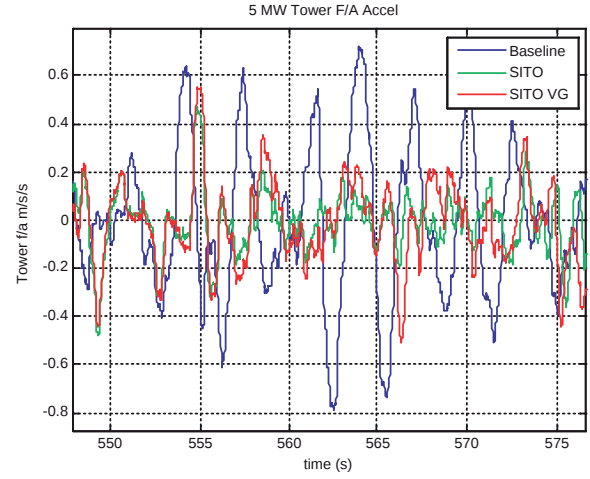


Figure 13: 5MW tower fore/aft acceleration for baseline, fixed gain and variable gain collective blade pitch control.

of absolute stability [20, 21].

To test the efficacy of a Nyquist-stable design for the 5 MW wind turbine, a SISO collective blade pitch/high speed shaft rate controller with this property is designed and tested in a turbulent wind environment. This controller has the advantage of the first tower mode control being abandoned (item 2 from section 3.2 is no longer a factor), so the bandwidth can be increased to 1 rad/s. Figure 14 show the Nichols plots without M-rings for the high speed shaft rate feedback only loop transmission (compensator is $G_{rate}(C_2)$ defined in section 2.4, the structure loop is opened) and the Nyquist-stable controller.

The Nyquist-stable controller is extremely aggressive. In addition to the steep roll off, the stability margins are smaller than those of the SITO rate regulation controller, and the torsion mode is phase stabilized as opposed to gain stabilized. These features, in addition to the extra octave of bandwidth, give the Nyquist-stable controller about 40 dB more feedback at 0.1 rad/s than the SITO rate regulator.

The aggressive features of the Nyquist-stable control system result in poor robustness. This is a particularly bad characteristic for wind turbine applications, as the plant dynamics vary with wind speed. Figure 15 shows the Nyquist-stable loop transmission for four different wind speeds in Region 3. While retaining stability at wind speeds higher than the design speed (18 m/s), there are critical point encirclements at both the initial crossover and the phase stabilized torsion mode. FAST simulations with this controller with turbulent wind frequently show violent oscillations within one minute of operation.

The Nyquist-stable robustness problem can be addressed via the application of gain-scheduling, however this is an arduous task. In addition to scheduling the linear controller, requisite nonlinear dynamic



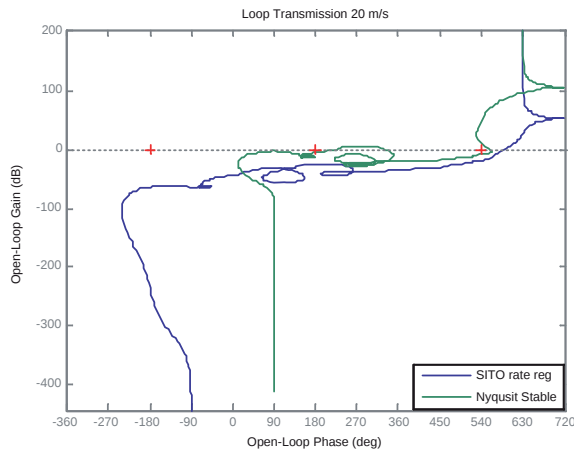


Figure 14: 5MW Nyquist-stable Loop Transmission Nichols Chart.

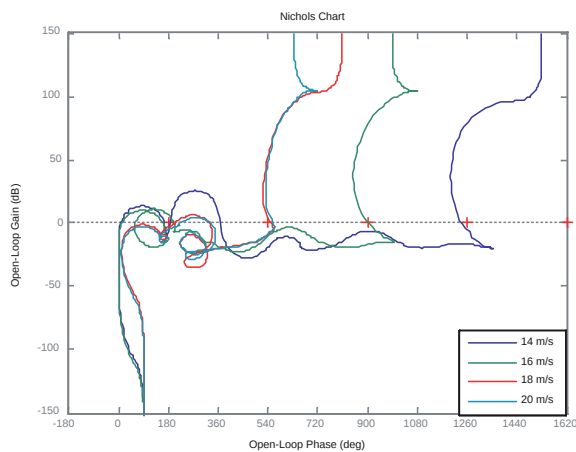


Figure 15: 5MW Loop Transmission Nichols Chart for Multiple Wind Speeds.

compensation for stability retention in actuator saturation would also have to be gain scheduled. The result would be a very complicated, perhaps fragile control system. In addition, the increase in performance at low frequency is offset by increased positive feedback near crossover, which might result in inferior SDRS performance. The advantages of Nyquist-stable control systems are realized when there is substantial feedback bandwidth, a characteristic that wind turbines do not have (item 1 from section 3.2). Thus, this type of aggressive, nonlinear control (Nyquist-stable control with nonlinear dynamic compensation) is ill-suited for wind turbines.

7 Results

A comparative analysis of available feedback for 600 kW and 5 MW wind turbines for multiple output, Region 3 control has been presented. Subject to feedback limitations that are driven by plant dynamics, it is determined that for the same loop transmission shape, the 5 MW high speed shaft rate regulator controller delivers 16 dB less feedback over a functional bandwidth of 0.1 rad/s. To present an alternative method of comparison, two performance measures are introduced using stochastic parameters and system responses: the standard deviation rated speed (SDRS) ratio indicates high speed shaft rate standard deviation normalized to rated shaft rate, and the tower fore/aft acceleration ratio (TFAAR), the ratio of closed loop to open loop tower fore/aft acceleration standard deviation. FAST simulation results show that while the CART2 and 5 MW controllers deliver comparable tower acceleration attenuation, the SDRS of the 5 MW controller is 4 times greater than that of CART2.

In an attempt to close the gap in performance, aggressive control strategies are applied to the 5 MW control problem. A rate error variable gain algorithm is applied to the 5 MW SITO controller, effectively trading away acceleration feedback for increased rate feedback to improve the SDRS. Using this approach, a 19% decrease in SDRS is achieved at a cost of a 16% increase in TFAAR. A Nyquist-stable controller is designed without acceleration feedback representing a more aggressive approach. While the feedback is substantially increased subject to the limitations identified, plant parameter variations due to changing wind speed drive this controller unstable. Extremely carefully designed gain scheduling and nonlinear dynamic compensation are required, and it appears that this system with its intrinsic robustness limitations would not be feasible for this application.

8 Conclusion

Frequency separated, two output control provides comparatively less rotor rate regulation for a 5 MW turbine compared to a 600 kW turbine. It is unknown what the consequence of this comparative lack of control performance using this design approach for the 5 MW wind turbines is. For instance, a constant torque turbine (e.g. CART2) requires high performance rotor rate regulation in Region 3. A rotor rate of 3% above rated speed is considered an overspeed event for this turbine. Aggressive feedback control is required to prevent overspeed events for this turbine in very windy conditions. As less feedback is available for the 5 MW turbine, it is unlikely that it can operate in very harsh conditions with comparable performance, as this analysis suggests. However, if the 5 MW turbines are operated in a constant *power* mode, then



the torque control can reduce generator torque as the turbine speed increases, reducing the stringent rate control requirement. The 5 MW turbines will be deployed offshore, so the increased rotor noise at these high rates is not an issue. This perhaps would allow more feedback to be applied for structural load mitigation.

Acknowledgements

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